7 ANALYSIS RESULTS

Due to the temperature limits on the spent fuel cladding, closure seals, impact limiter core materials, and neutron shield core materials, these components are the most important elements to consider in evaluating the response of the transport systems to the fire scenario. The peak cladding temperature limit is important because the cladding is the primary fission product containment boundary for the spent fuel. The temperature limit for the closure seals is important because these seals constitute the outer-most containment boundary for the package. The temperature limits for the neutron shield material and impact limiters are important because these materials are the most vulnerable to damage or destruction during the fire. The results of the analyses for the three rail packages were evaluated primarily in relation to the peak predicted temperatures for these components in the fire transient.

These analyses indicate that the spent fuel cladding reaches a peak temperature of 845°F (452°C) in the TN-68 package, 930°F (499°C) in the HI-STAR 100 package, and 884°F (473°C) in the NAC LWT system. Peak cladding temperatures for the TN-68, HI-STAR 100, and NAC LWT packages are below the currently accepted short term temperature limit[17] of 1058°F (570°C) for Zircaloy clad spent nuclear fuel under accident conditions [23].

The transient results for each of the three systems are discussed in detail below. Section 7.1 discusses the response of the TN-68 package during the fire. Section 7.2 presents results for the HI-STAR 100 package. Section 7.3 discusses the response of the NAC LWT package.

7.1 TN-68 Fire Transient Results

The COBRA-SFS model of the TN-68 package consists of a total of 530,228 computational nodes that are solved for each time step. This yields an overwhelming volume of output that must be sorted, sifted, and processed to produce a coherent picture of the response of the package to this fire scenario. The following three subsections present the peak temperatures versus time for selected components, as determined with COBRA-SFS for the TN-68 package subjected to the fire transient conditions described in detail in Section 6. The results are presented separately for the three main phases of the transient. Section 7.1.1 discusses the predicted response of the TN-68 package during the fire. Section 7.1.2 presents results for the post-fire transient over the duration of the NIST simulation (to 30 hours). Section 7.1.3 discusses the response to the postulated long-term post-fire conditions, out to 300 hours.

7.1.1 TN-68 During the Fire

Figure 7.1 shows the initial temperature response of the TN-68 package predicted with COBRA-SFS during the fire portion of the transient. The fire burns for the first 6.75 to 7 hours of the transient (see Figures 6.1 and 6.2 for boundary temperatures representing the fire with the package 66 ft (20 m) from

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[17] The short-term temperature limit of 1058°F (570°C) is based on creep experiments performed on two fuel cladding test samples which remained undamaged (i.e., no significant observable damage) when held at 1058°F (570°C) for up to 30 and 71 days [24]. This temperature limit is a relatively conservative limit, since the temperature at which Zircaloy fuel rods actually fail by burst rupture is approximately 1382°F (750°C)[25].

the fire center). During this time, the outer surface temperature of the package shell increases quite rapidly. The maximum temperature of the package surface increases at a rate of up to 10.5°F/min. (5.8°C/min.), reaching a peak temperature of 1789°F (976°C) at about 6.3 hours into the fire. The maximum temperature of the neutron shield material also shows a relatively rapid increase, reaching a peak of 1355°F (735°C) at approximately 6.9 hours into the fire.

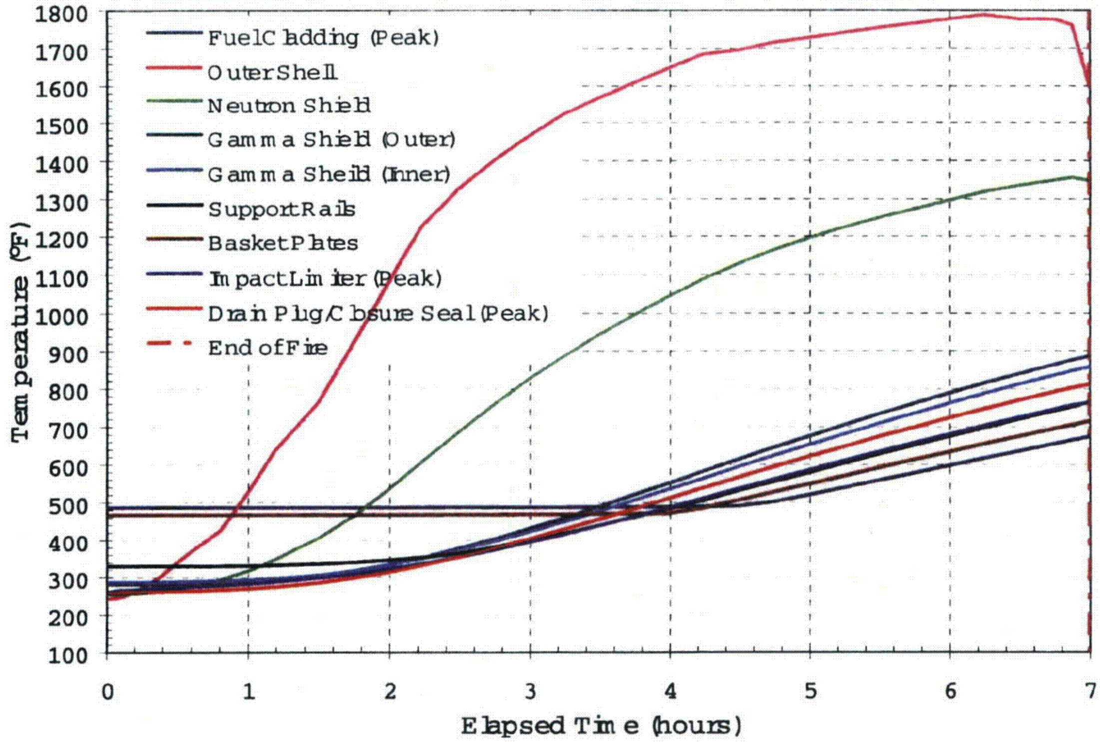


Figure 7.1. Maximum Temperature Histories for TN-68 Components During Fire Transient

The internal components of the package show a very slow thermal response during the fire. The support rails and gamma shields take more than an hour to show any noticeable increase in temperature. Approximately 3.5 hours elapse before the peak fuel cladding temperature rises as much as 1°F above the initial steady-state peak temperature of 485°F (252°C). The peak temperatures of the basket tubes and poison plates rise only about 4°F (2.2°C) in the first four hours of the fire. During this time period, the peak temperature on the outer shell of the package is predicted to go up to 1647°F (897°C), the predicted peak temperature of the neutron shield rises to 1042°F (561°C), and the peak temperature on the gamma shields increases to 549°F (287°C).

By the end of the fire, marking the point at which all volatile flammables are consumed, the peak clad temperature has risen to only 673°F (356°C), and the peak temperature of the basket tubes and poison plates is at about 714°F (379°C). The outer shell of the package is predicted to have a peak temperature of 1599°F (871°C) at the end of the fire, with the neutron shield at 1347°F (731°C) and the outer gamma shield at 886°F (474°C).

Figure 7.2 shows midline temperature profiles from top to bottom vertically through the package cross-section, including the package shell, support rails, and basket structure during the fire at approximately hourly intervals. The large difference in the predicted rate of increase in temperature for the internal and external components of the package is illustrated by these profiles. The temperatures of the nodes modeling the basket tubes and poison plates change very little during the fire. The support rails and gamma shield nodes heat up relatively slowly, while the outer shell and neutron shield region increase rapidly in temperature in response to the fire.

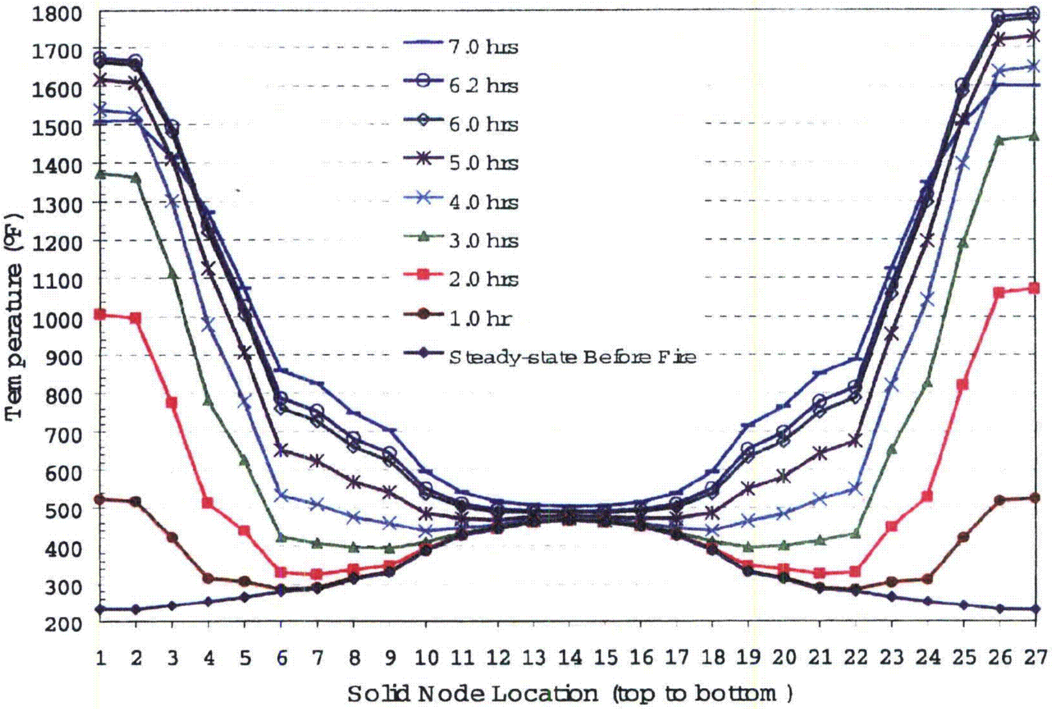


Figure 7.2. Temperature Profiles Top-To-Bottom Through TN-68 Package During Fire Transient

The neutron shield and gamma shields insulate the basket and fuel assemblies from the fire, but the slow response is also due in large part to the huge thermal inertia of the package components. The 68 assemblies within the package weight on the order of 20 to 25 metric tons (mainly uranium dioxide and Zircaloy), with roughly 8 metric tons of material in the basket (mainly steel and borated aluminum). The inner and outer gamma shields consist of approximately 40 metric tons of carbon steel, while the outer shell of the package is approximately 5 metric tons of steel. Even under the severe heat load imposed by the sustained high temperatures of a fire lasting almost 7 hours, it takes time to raise the temperature of such a large mass of material, even with its internal heat generation due to the spent fuel assemblies.

A significant detail discovered during the evaluation of the TN-68 is that during the first quarter of the fire transient, the total heat flux associated with radiation heat transfer from the tunnel to the package is nearly an order of magnitude greater than the total heat flux associated with convection heat transfer from the tunnel environment to the package. As a consequence, the most severe conditions for this transient are those that result in the package receiving the greatest exposure to radiation heat transfer. This means that a horizontal orientation will result in the greatest possible heat input for a given fire scenario. Any package orientation other than the horizontal orientation during the fire (e.g., the package up-ended into a vertical orientation as a result of an accident) would yield less severe heat input to the package.

7.1.2 TN-68 Short-Term Post-Fire Response

Figure 7.3 shows the temperature response of the package during the first 30 hours of the COBRA-SFS simulation. (This time period represents the total duration of the NIST analysis that is the source of the boundary conditions for this calculation.) During the fire, the material in the neutron shield reaches temperatures that would heavily degrade the borated polyester. This does not mean, however, that the package would fail to meet the requirement of maintaining appropriate shielding in this scenario. This spent fuel transportation package is expected to lose its neutron shield material in the fire accident specified in current regulations, and therefore the design does not rely on the neutron shield material remaining intact in order to maintain shielding. This package is designed to attenuate neutron radiation to acceptable levels (see 10 CFR 71.51 [1]) following an accident without the assistance of the neutron shield material. However, the loss of the shield material means that the neutron shield's heat transfer capability would be expected to deteriorate rapidly during the fire.

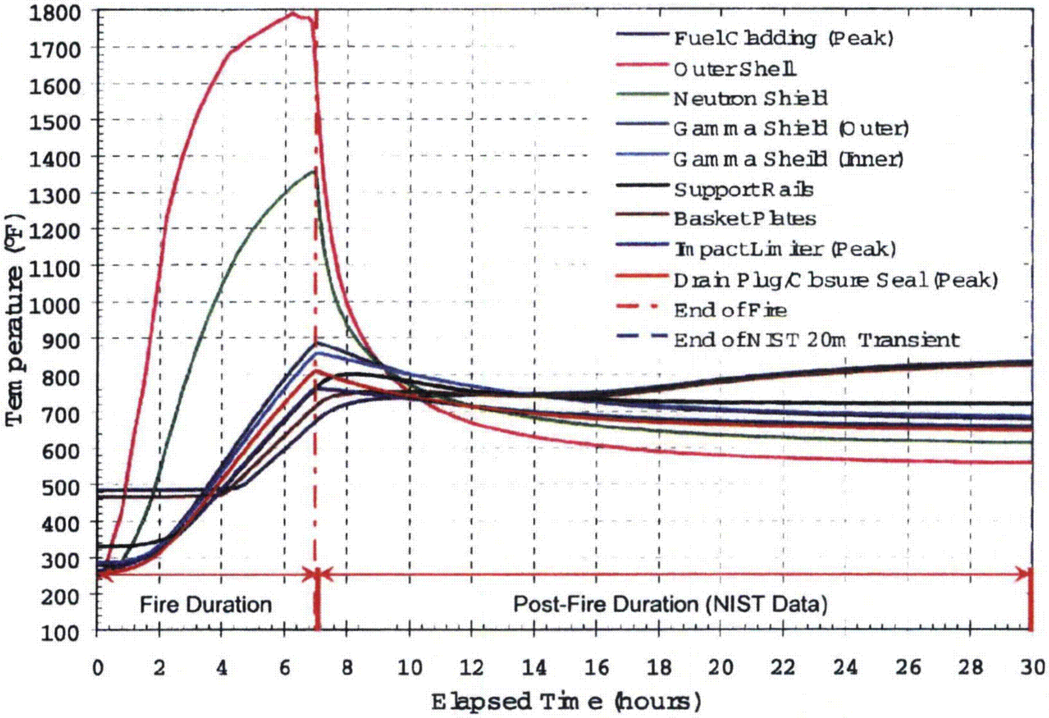


Figure 7.3. Maximum Temperature Histories for TN-68 Package Components During First 30 hr of Transient

In the COBRA-SFS evaluation, it was assumed that the borated polyester remains in place and is unaffected during the fire, but instantly degrades at the end of the fire and is replaced by hot air. This maximizes the heat input into the package during the fire, then imposes an additional barrier to heat transfer from the package after the fire. From the standpoint of the thermal response of the system, this is a conservative representation of the effect of the fire on the neutron shield. The thermal conductivity of the borated polyester is about 140 times that of air, so extending the residence time of the polyester to the end of the fire results in a calculation that overestimates the rate of heat flow into the package during the fire. This will tend to result in higher calculated temperatures on the package internals than would be obtained if it were assumed that the polyester was replaced with air earlier in the transient. In reality, the change would be more gradual and would occur earlier in the transient as the neutron shield burned away. The heat absorbed in the process of consuming the polyester material is not subtracted from the heat of the fire, as an associated conservatism.

The temperatures in Figure 7.3 show that once the fire is over, the peak temperatures on the package shell and neutron shield are predicted to begin to drop precipitately. This is primarily a response to the rapid decrease of the boundary temperatures, as can be seen in Figure 7.4. This plot shows the outer shell surface temperature predicted with COBRA-SFS compared to the tunnel ceiling temperature and the temperature of the air above the package, which are derived from the NIST calculations and used as boundary conditions.

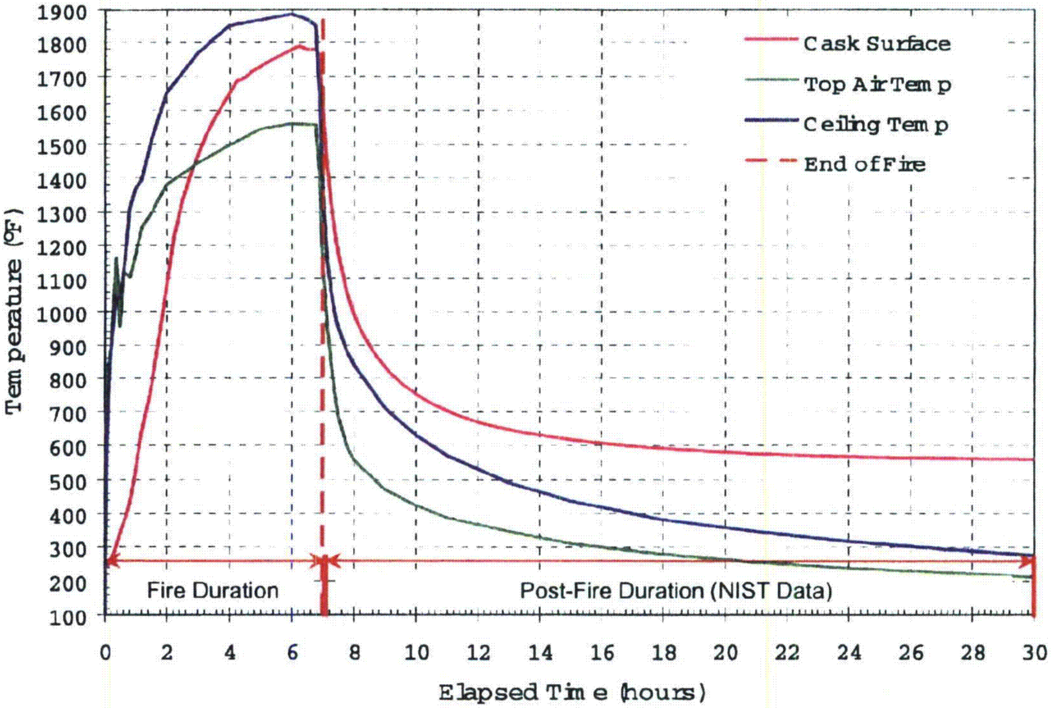


Figure 7.4. Maximum TN-68 Package Surface Temperatures Compared to NIST Boundary Condition Temperatures

Figure 7.3 also shows that the peak temperatures on the gamma shields and support rails of the package decrease after the fire ends. However, the temperature decrease for these components is much slower than for the neutron shield region and outer shell because the internal components must also absorb the thermal load from the fuel. For the same reason, the peak temperature of the basket shows a continuous increase even after the end of the fire, as does the peak clad temperature.

The plot of the predicted peak clad temperature in Figure 7.3 shows that the thermal output of the fire itself does not have much of an effect on the fuel. The observed rise in peak clad temperature is mainly a response to the effect of the external boundary conditions on the rate of heat transfer from the package. The heat of the fire does not result in much of an increase in the package internal temperatures, but the increase in the external air temperature severely compromises the rate of heat rejection from the package and continues to do so long after the fire is out. This is illustrated very clearly by the plot of the global peak clad temperature alone, shown in Figure 7.5 for 50 hours of the transient (i.e., the 30 hours of the NIST transient, plus an additional 20 hours of the extended cool down beyond the NIST calculation).

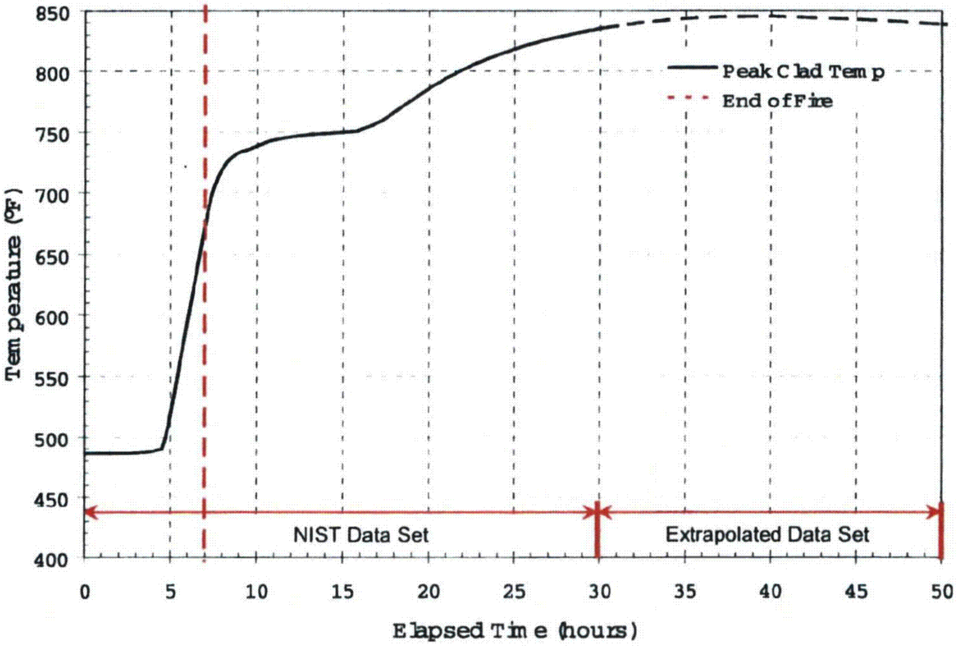


Figure 7.5. Peak Fuel Cladding Temperature History in TN-68 During First 50 hr of Transient

As shown by the plot in Figure 7.5, at about 4.5 hours into the fire the peak clad temperature begins an almost adiabatic heat up (approximately 77°F/hr (43°C/hr)) because the fire is preventing normal heat removal from the package (which occurs by thermal radiation to the external environment and natural convection at the surface). This adiabatic heat-up continues for about an hour after the end of the fire, until the package shell temperature drops low enough to permit some heat removal from the package by radiation to the tunnel surfaces.

The fuel cladding temperature increase observed shortly before the end of the fire (at ~4.5 hours) occurs in fuel in the outer periphery of the basket, because the fire is heating the outer cylinder of the package. This causes the peak fuel cladding temperature to shift first to the bottom assembly in the horizontal basket cross-section, then to the top assembly. The rate of increase in the predicted peak cladding temperature slows briefly to 1-2°F/hr (0.5-1°C/hr) before increasing again as the peak temperature location shifts back to the center assembly in the core of the basket. (The peak fuel clad temperature location is automatically tracked in these results during the transient.)

The peak clad temperature continues to increase after the fire, because thermal radiation from the tunnel ceiling, walls and floor is continuing to add heat to the package and the external ambient air temperature slows the rate of heat removal by convection. However, the rate of increase drops to only about 2°F/hr (~1°C/hr). At about 15 hours into the transient, the rate of increase of the peak clad temperature begins to climb again, to a rate of about 10°F/hr (6°C/hr), then again drops to only 3°F/hr (~2°C/hr) after about 20 hours. This behavior is due to the decrease in the rate of heat removal via thermal radiation as the tunnel surfaces cool down, and the more gradual decrease in the rate at which heat is being removed from the package by forced convection, due to the decrease in velocity and temperature of the hot air flowing past the package.

By the end of the NIST transient at 30 hours, the rate of increase of the peak clad temperature has dropped to less than 3°F/hr (<2°C/hr). The global peak cladding temperature of 845°F (452°C) is finally reached at ~40 hours into the transient. (The dashed portion of the curve in Figure 7.5 denotes results for boundary conditions extrapolated beyond the results obtained in the 30-hour NIST calculation.)

The maximum temperature history of the seals in the package closure and vacuum port is shown in Figure 7.6. The curve in this figure represents the global peak of all seal material used in the TN-68. As shown in this figure, the Helicoflex® seal material is predicted to reach a maximum temperature of 811°F (433°C) right at the end of the fire, then gradually decreases as the transient proceeds into the post-fire cool down. This peak temperature exceeds the seal manufacturer's recommended maximum long-term service temperature of 536°F (280°C) for this material, as specified in the SAR [11]. (For a discussion of the consequences of seal failure, see Section 8.2.1.)

Bolts and other subcomponents were not explicitly represented at the package ends in the COBRA-SFS model of the TN-68. However, the depicted temperature history (see Figure 7.6) conservatively represents the peak temperature history of the closure bolts due to the manner in which heat must migrate around the top impact limiter into the package upper forging, through the closure seal location, and then into the closure. This is due to the limited conduction offered by the steel-encapsulated wooden impact-damping material comprising the impact limiter.

The thermal response of the package after the end of the fire is further illustrated in Figure 7.7, with plots showing radial temperature profiles through the package at selected time intervals through the transient. These profiles show that the outer shell and former neutron shield cool rapidly once the fire is over, while the temperatures of the internal nodes representing the basket tubes and poison plates continue to rise in response to the heat load from the spent nuclear fuel.

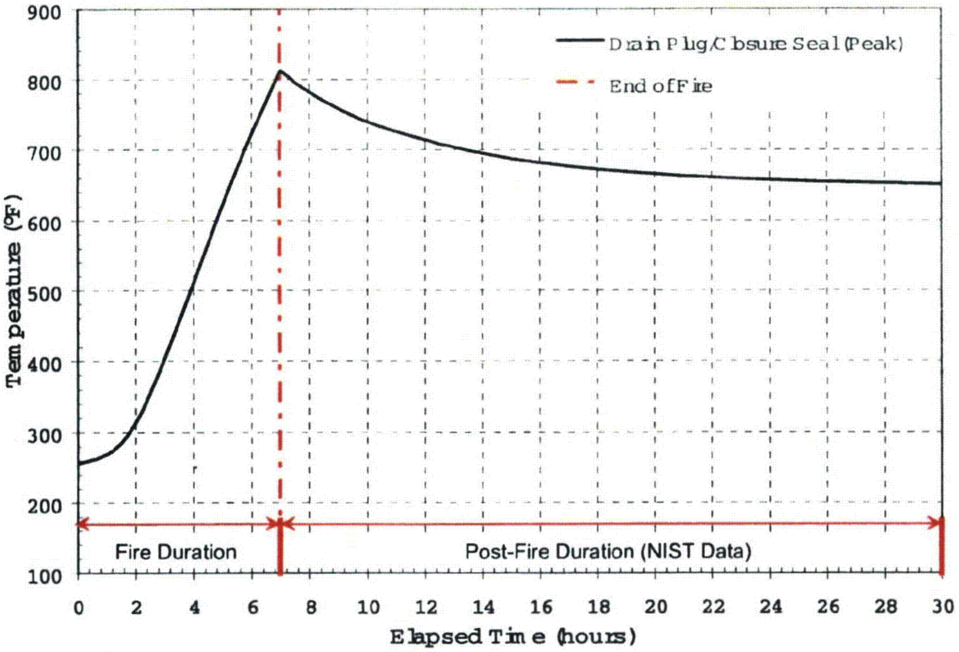


Figure 7.6. Maximum Global TN-68 Closure/Port Seal Temperature History for 30-hr Transient

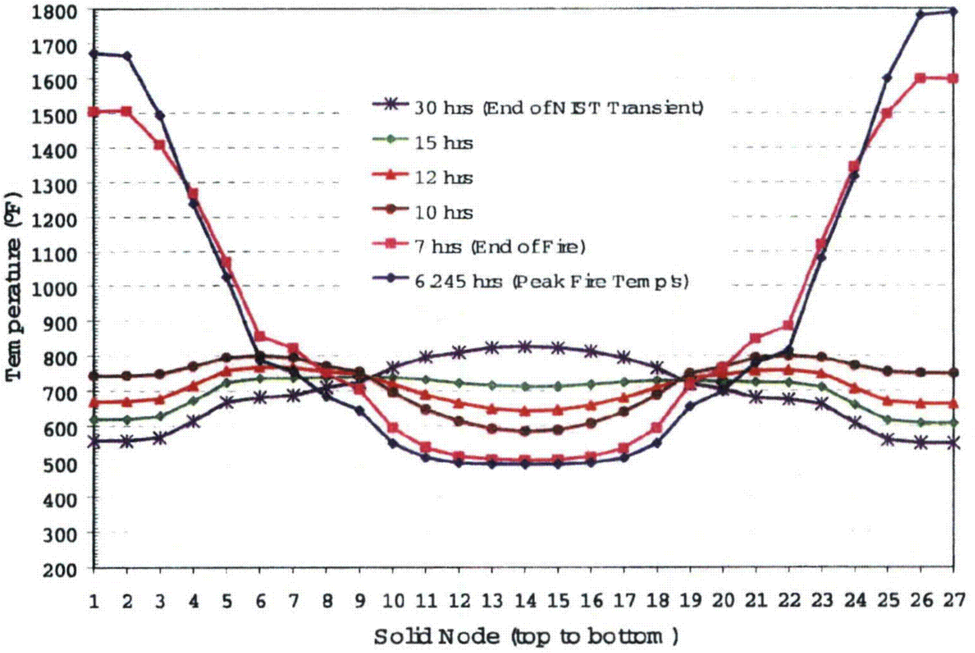


Figure 7.7. Temperature Profiles Top-To-Bottom Through TN-68 Package Axis to 30 hrs

7.1.3 TN-68 Long-Term Post-Fire Response

The NIST calculation used to define the boundary conditions for the COBRA-SFS analysis simulated the fire transient and its aftermath out to 30 hours. However, the trends exhibited by the temperatures of the various components of the package at the end of the transient indicate that the system is not yet at a new steady state by then. Temperatures predicted by NIST for the first 30 hours were extrapolated from 30 hours out to 300 hours using a power function to realistically model cool down of the tunnel environment. (The extrapolated values are presented in Figures 6.5 and 6.6 for the air and wall temperatures, respectively.)

To explore the effects of prolonged exposure to post-fire conditions in the tunnel, the calculations were carried out for the full 300 hours (293 hours after fire cessation). This is equivalent to assuming that the package will be left in the tunnel for an extended period (days or weeks rather than merely hours) without any emergency responder intervention. This assumption is not realistic but is highly conservative, defining a severe long-term ambient environment around the package.

During the first 30 hours of the transient, the heat transfer at the package surface was assumed to be forced convection at the velocities predicted in the tunnel by the NIST calculation. The basis for this approach is discussed in Section 6.0. This is a relatively conservative assumption, particularly for conditions after about 20 hours into the transient, when the velocities predicted in the NIST calculation have dropped to values of 1 to 2 ft/s (0.3 to 0.6 m/s) or less. For the latter portion of the transient ($t \geq 20$ hours), heat transfer at the package surface is a complex mixture of forced convection (due to air flow induced in the tunnel by the vertical temperature gradients from the fire) and free convection (driven by the non-uniform circumferential temperatures of the package outer shell). This was conservatively approximated by imposing a purely forced convection heat transfer coefficient (based on the NIST air velocities and temperatures) for the first 30 hours of the simulation, then imposing a free convection coefficient for the remainder of the calculation.

Figure 7.8 shows the temperature response of the various components of the package for the long term transient calculation to 300 hours. (As previously indicated, the dashed portion of the curve is used to distinguish the results that stem from the boundary conditions that were extrapolated from the NIST simulation.) As shown in Figure 7.8, the highest peak clad temperature, 845°F (452°C), is reached at approximately 40 hours. The peak temperature for the basket structure is also reached at about the same time. The predicted maximum in the peak clad temperature is below the regulatory limit of 1058°F (570°C) by a difference of 213 °F (110 °C). All other temperatures in the package have been decreasing steadily since the end of the fire.

By 100 hours, the peak clad temperature has dropped to 784°F (418°C), with temperatures decreasing at rates of about -1 °F/hr (-0.6 °C/hr). After 200 hours, the peak clad temperature has dropped to 719°F (382°C), and at 300 hours is predicted to be down to 690°F (366°C) for the specified boundary conditions. At this point in the transient calculation, the rate of change of local temperatures in the system is about -0.2°F/hr (-0.1°C/hr). The rate of cooling is very slow due to the huge thermal mass of the package and its fuel load. Projections of the cooling rate indicate that it would take an additional 175-200 hours to reach a new post-fire steady state.

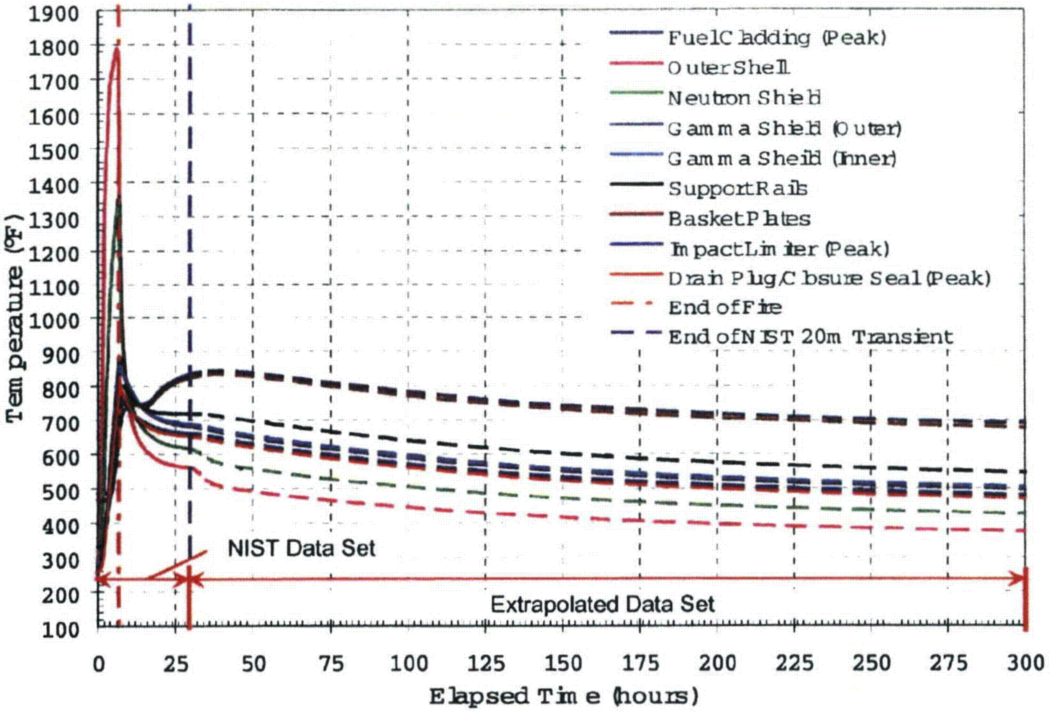


Figure 7.8. Maximum Temperature Histories for TN-68 Package Components During 300 hr of Transient

The trends in Figure 7.8 show that the overall thermal response of the package to the fire transient is essentially an accommodation to the new higher-temperature boundary conditions extrapolated from the conditions predicted at the end of 30 hours in the NIST fire analysis. The temperature of the fuel and basket is largely unaffected by the heat input to the package from the fire; the increase in peak clad temperature and peak basket temperature is due almost entirely to having no heat removal from the package during the fire and for about an hour immediately afterwards. After the ambient temperatures drop enough to allow heat removal from the package, the rate of increase of the peak clad temperature begins to level off and then finally turn around about 40 hours into the transient.

Viewed on the scale of 300 hours, the fire portion of the transient appears as a large but relatively short-lived spike in the boundary conditions that significantly affects only the outer shell, neutron shield, and impact limiters, and to a lesser extent the outer and inner gamma shielding. These components show a rapid temperature increase during the fire, but after the end of the fire immediately begin a rapid cool down. Peak component temperatures for the TN-68 package in the fire simulation are summarized in Table 7.1.

Table 7.1. **TN-68 Peak Component Temperatures During Fire Transient**

Component	Maximum Temperature (COBRA-SFS) °F (°C)	Time (hours)
Fuel Cladding	845 (452)	40
Basket Plate	836 (447)	40
Basket Rail	801 (427)	8.3
Inner Shell	857 (458)	7.0
Gamma Shell	886 (474)	7.0
Package Bottom	762 (406)	7.0
Seals	811 (433)	7.0
Neutron Shield	1355 (735)	6.9
Outer Shell	1789 (976)	6.3

7.2 Holtec HI-STAR 100 Fire Transient Results

The ANSYS model of the HI-STAR 100 package consists of a total of 149,100 standard computational elements and 288 superelements that are solved for each time step. Similar to the COBRA-SFS model of the TN-68, this model yields an overwhelming volume of output that must be processed to produce a coherent picture of the package response. The following three subsections present the peak temperatures versus time for selected components, as determined with ANSYS for the HI-STAR 100 subject to the hypothetical fire transient conditions described in Section 6.

7.2.1 HI-STAR 100 During the Fire

Figure 7.9 shows the initial temperature response of the HI-STAR 100 package predicted with ANSYS during the fire portion of the transient. The maximum temperature of the HI-STAR 100 package surface increases rapidly to a peak temperature of 1831°F (999°C) around 6 hours into the fire. The maximum temperature of the inner shell material, which defines the primary containment boundary, also shows a relatively rapid increase, reaching a peak of 1447°F (786°C) approximately 6.75 hours into the fire. This corresponds to the peak boundary condition temperatures defined by the fire. The fire temperatures predicted in the NIST analysis peak at 6.75 hours, and then drop off rapidly thereafter as the fire burns itself out. The peak temperature of the inner shell material is predicted to lead the gamma shield material peak temperature because the elements selected to define the primary containment boundary include the bottom and top forgings and lid. A large section of the top forging is directly exposed to the fire (un-shrouded by the gamma shield, neutron shield/fin section and upper impact limiter).

Similar to the TN-68 results with the COBRA-SFS model, the internal components of the HI-STAR 100 package also show a very slow thermal response during the fire. The gamma shield takes more than half an hour to show any noticeable increase in temperature. Nearly three hours elapse before the internal canister shell temperature rises as much as 1 °F (0.6 °C) above the initial steady-state peak temperature of 548°F (287°C). In the first five hours of the fire, the peak temperatures of the basket structure, poison plates, and fuel rise only by about 2 °F (1 °C). This is approximately an hour later than the TN-68 response and can be attributed to the additional thermal barrier of the MPC canister. In these five hours, the peak temperature on the outer skin surrounding the neutron shield of the HI-STAR 100 is predicted to go up to 1809°F (987°C), and the peak temperature on the gamma shields increases to 1332°F (722°C).

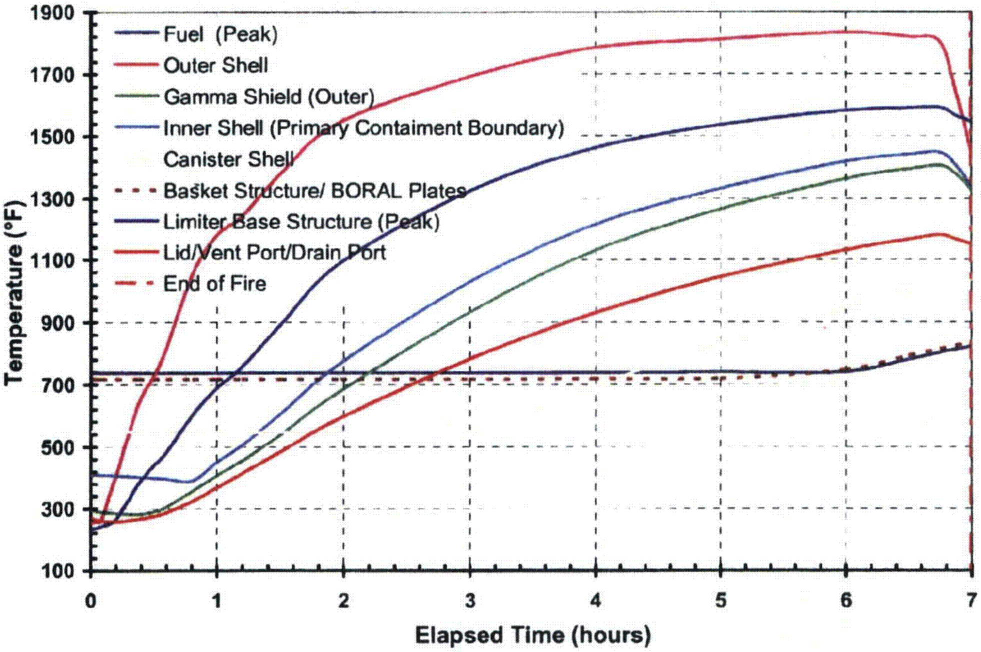


Figure 7.9. Maximum Temperature Histories for HI-STAR 100 Package Components During Fire Transient

By the end of the fire, marking the point at which all volatile flammables are consumed, the peak clad temperature has risen to only 803°F (428°C). The outer shell of the package is predicted to have a peak temperature of 1801°F (983°C) at the end of the fire, with the outer gamma shield at 1404°F (762°C). This is a bit warmer than the TN-68 at this point in time. The difference is due mainly to the larger thermal resistance to radial heat flow in the thinner skin of the HI-STAR 100, compared to the TN-68. However, both packages tend to perform similarly overall.

As with the TN-68 results, the large difference in the predicted rate of increase in temperature for the internal and external components of the HI-STAR 100 is because the neutron shield and gamma shield insulate the basket and fuel assemblies from the fire. The slow response is due mainly to the huge thermal inertia of the package components themselves. Even under the severe heat load imposed by the sustained high temperatures of a fire lasting nearly 7 hours, it takes time to raise the temperature of such a large mass of material, despite its internal heat generation component.

7.2.2 HI-STAR 100 Short-Term Post-Fire Response

Figure 7.10 shows the temperature response of selected components of the package during the first 30 hours of the ANSYS transient simulation. During the fire, the material in the neutron shield is predicted to achieve temperatures that will heavily degrade it. As noted in Section 7.1.1 for the predicted loss of the TN-68 package's neutron shield, the HI-STAR 100 is also designed to attenuate neutron radiation to acceptable levels (see 10 CFR 71.51 [1]) following an accident without the assistance of the neutron shield material. However, the neutron shield's heat transfer capability is expected to deteriorate rapidly during the fire. In the ANSYS evaluation, it was assumed that the neutron shield material (HOLTITE-A) remains in place and unaffected during the fire, but instantly degrades at the end of the fire, to be replaced by hot air. This maximizes the heat input into the package.

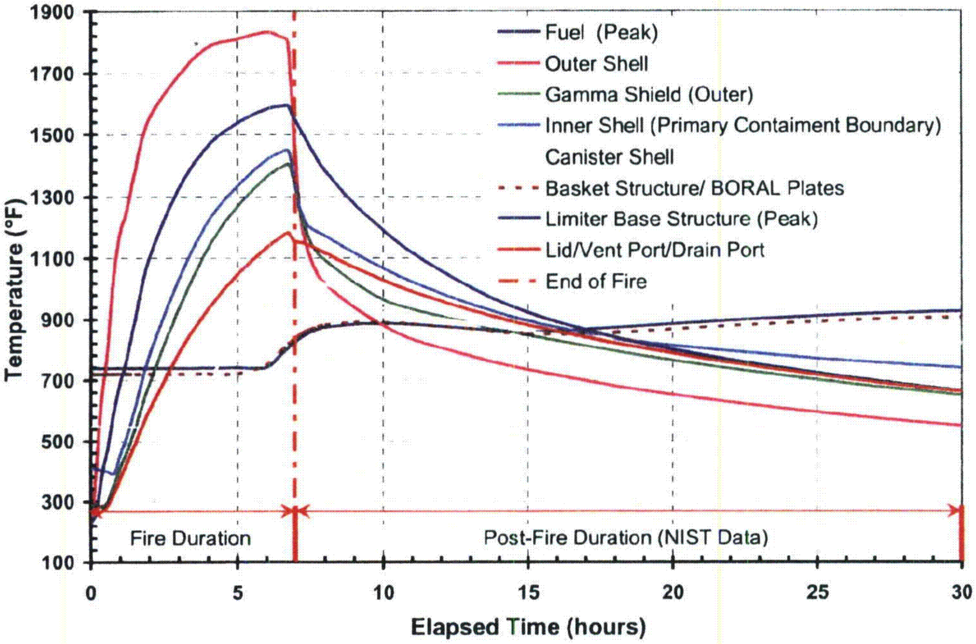


Figure 7.10. Maximum Temperature Histories for HI-STAR 100 Package Components During First 30 hr of Transient

This is a conservative representation of the effect of the fire on the neutron shield from the standpoint of the thermal response of the system. The thermal conductivity of HOLTITE-A is approximately 16 times that of air, so extending the residence time to the end of the fire results in the calculation somewhat overestimating the rate of heat flow into the package during the fire. This will result in higher calculated temperatures on the package internals than would occur if degradation were accounted for at a more realistic rate during the fire. As an additional conservatism, the latent heat absorbed in the degradation of the material, which would tend to decrease the external heat flux due to the fire, is also neglected.

The results shown in Figure 7.10 demonstrate that once the fire is over, the predicted peak temperatures on outboard components begin to drop rapidly (i.e., outer shell, gamma shield, etc.). This is primarily a response to the rapid decrease of the boundary temperatures, as can be seen in Figure 7.11, which shows the outer shell surface temperature predicted with ANSYS compared to the tunnel ceiling temperature and the temperature of the air above the package derived from the NIST calculations and used as boundary conditions.

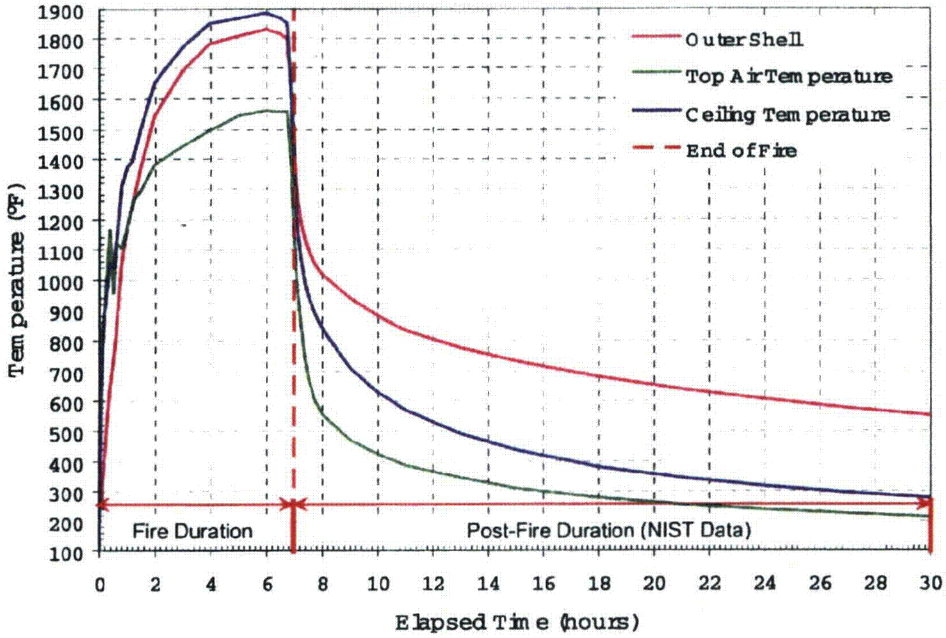


Figure 7.11. Maximum HI-STAR 100 Package Surface Temperature Compared to NIST Boundary Condition Temperatures

Figure 7.10 shows that the peak temperatures on the gamma shields and inner shell of the package decrease after the end of the fire. However, the temperature decrease for these components is much slower than for the outer shell because the internal components must absorb the thermal load from the fuel. Similarly, the peak temperature of the basket shows a continuous increase for nearly three hours after the end of the fire, as does the peak clad temperature.

The plot of the predicted peak clad temperature in Figure 7.10 shows that the thermal output of the fire itself has little effect on the fuel or its accommodating basket. As discussed for the TN-68 in Section 7.1.3, the observed rise in peak clad temperature is mainly a response to the effects of the external boundary conditions on the rate of heat transfer from the package. The heat of the fire does not result in much of an increase in the package internal temperatures, but the increase in the external air temperature severely compromises the rate of heat rejection from the package, and continues to do so long after the fire is out. This is illustrated very clearly by the plot of the peak clad temperature alone shown in Figure

7.12 for the first 50 hours of the NIST transient. The fire is very nearly over before the peak clad temperatures show a discernable increase.

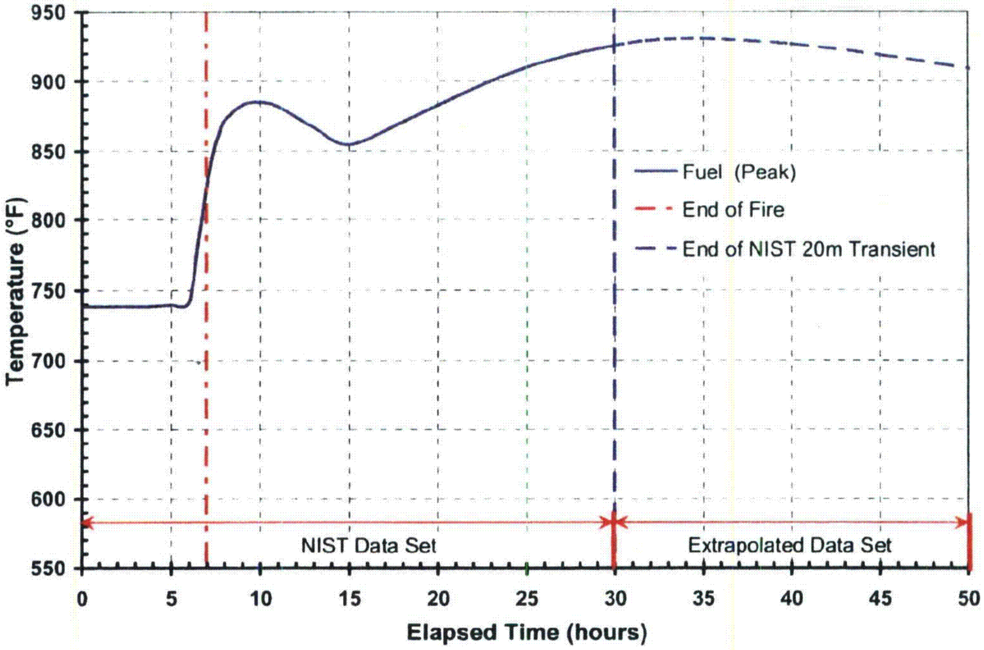


Figure 7.12. Peak Fuel Clad Temperature History in HI-STAR 100 During First 50 hr of Transient

Figure 7.12 shows that a little after 5 hours into the fire, the global peak clad temperature begins an almost adiabatic heat up (approximately 69°F/hr (38°C/hr)) because the fire prevents normal heat removal from the package by natural convection at the surface. This adiabatic heat-up continues for about an hour after the end of the fire, until the package shell temperature drops low enough to permit some heat removal from the package by radiation to the tunnel surfaces.

The initial fuel cladding temperature rise shown to initiate shortly before the end of the fire (at approximately 6 hours) occurs on rods in the outer periphery of the basket in the portion of the package facing the top of the tunnel. The fuel in this region is initially rising in temperature faster than that residing in the center of the basket. This continues until about 8 hours into the transient. However, by this time the fire has been over for more than an hour, and internal component temperatures are redistributing radially throughout the package, causing the peak fuel cladding temperature to shift from one assembly to another. The peak cladding temperature drops for a brief period, due to this internal spreading of heat, but by about 15 hours begins to rise again as fuel in the center of the basket heats up enough to exceed the temperature of the fuel on the outer periphery. (Just as with the TN-68 results, the peak fuel clad temperature is captured in the global summary as it moves from assembly to assembly within the fuel basket during the transient.)

The peak clad temperature continues to increase, because the hot air flow and hot tunnel surfaces resulting from the fire are continuing to compromise heat rejection from the package surface. The package is designed to reject heat to ambient at 100°F (38°C), but the air within the tunnel environment is still above 200°F (93°C) at 30 hours, decreasing from a peak of 1557°F (847°C) at the end of the fire. However, by the end of 30 hours, the rate of increase in the peak clad temperature has dropped to only about 2 °F/hour (1 °C/hour), in response to the decreasing boundary temperatures. The global peak cladding temperature reaches a maximum of 930°F (499°C) at approximately 35 hours into the transient.

The maximum temperature history of the seals in the package lid closure, ports, and port covers is shown in Figure 7.13. The curve in this figure represents the global peak of all seal material utilized in the HI-STAR 100. These temperatures are gathered by querying nodes at the seals' locations, even though the seals were not explicitly represented in the model. As shown in this figure, the maximum temperature in these regions is 1181°F (638°C), reached at the end of the fire. The maximum temperature then gradually begins to decrease as the transient proceeds into the post-fire cool down. Despite an abrupt rise in temperature during the fire, the peak temperature in the seal region remains below the lowest reported maximum continuous-use temperature limit of 1200°F (649°C) for the metallic mechanical seal material. (See Table 4.1.1 of the SAR [10] for the HI-STAR 100.)

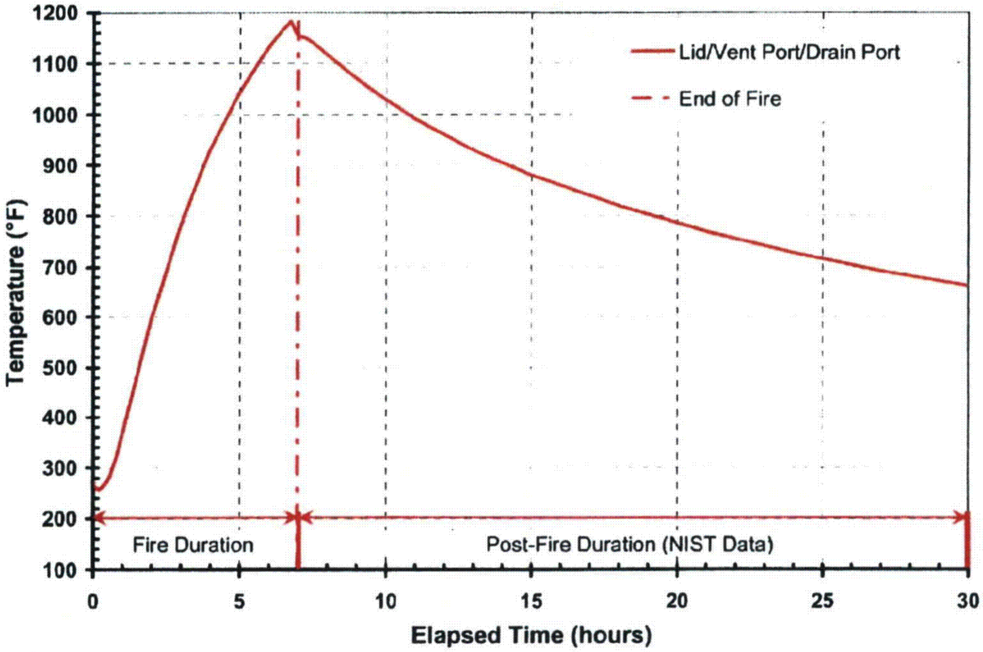


Figure 7.13. Maximum Global HI-STAR 100 Closure/Port Seal Temperature History During First 30 hr of Transient

Bolts were not explicitly represented at the package lid and buttress interface in the ANSYS model of the HI-STAR 100. However, the depicted seal temperature history conservatively represents the peak temperature history of the closure bolts due to the manner in which heat has to migrate around the top impact limiter, into the package upper forging (between the top limiter and neutron shield/fin section), through the closure seal location, and then into the closure. This is due to the limited conduction offered by the stainless steel-encapsulated cellular honeycomb material.

7.2.3 HI-STAR 100 Long-Term Post-Fire Response

The trends exhibited by the temperatures of the various components of the HI-STAR 100 package at the end of the 30-hour transient indicate that the system is not yet at a new steady state. Boundary temperatures predicted by NIST were extrapolated from 30 hours out to 300 hours using a power function to realistically model cool down of the tunnel environment. (The extrapolated values are presented in Figures 6.5 and 6.6 for the air temperatures and wall temperatures, respectively.)

To explore the effects of prolonged exposure to post-fire conditions in the tunnel, the calculations were carried out for the full 300 hours (273 hours after fire cessation). As discussed previously, this is equivalent to assuming that the package will be left in the tunnel for up to 12.5 days without any emergency response. This assumption is not realistic, but is highly conservative, defining a relatively severe long-term ambient environment around the package.

The same conservative assumptions applied to the TN-68 evaluation for external convection during the fire and post-fire duration were applied to the HI-STAR 100 evaluation. A purely forced convection heat transfer regime and associated heat transfer coefficient was assumed for the first 30 hours of the simulation, then a purely free convection regime and associated coefficient was assumed for the remainder of the calculation ($t \geq 30$ hours). (Refer to Section 6 for detailed discussion of the heat transfer boundary conditions on the package surfaces.)

Figure 7.14 shows the temperature response of the various components of the package for the long term transient calculation to 300 hours. As previously discussed, the dashed portion of the curve is used to distinguish the results obtained with boundary conditions that were extrapolated from the NIST calculation. As noted in Section 7.2.2 (see Figure 7.12), the highest peak clad temperature is reached at approximately 35 hours, with a value of 930°F (499°C). This is 128 °F (71 °C) below the regulatory limit of 1058°F (570°C) for accident conditions. The peak temperature for the basket/poison plate structure is reached at about the same time.

All other temperatures in the package decrease steadily after the end of the fire. By 100 hours, the peak clad temperature has dropped to 817°F (436°C). Similar to the results for the TN-68, this system is not yet at a new post-fire steady-state by this time (see Section 7.1.3, Figure 7.8). The HI-STAR 100 is nearing its new post-fire steady-state at about 200 hours, with rates of temperature change on the order of approximately -0.3 °F/hr (-0.2 °C/hr). After 250 hours, the peak clad temperature has dropped to 747°F (397°C), and at 300 hours, it is predicted to be 740°F (393°C) for the specified boundary conditions. At this point in the transient calculation, the rate of decrease in local temperatures in the system is less than 0.1 °F/hr (0.06 °C/hr), and the conditions can be considered as essentially a new post-fire steady state.

The trends in Figure 7.14 show that the overall thermal response of the package to the fire transient is essentially an accommodation to the new higher temperature boundary conditions represented by the conditions predicted at the end of 30 hours in the NIST fire analysis. Viewed on the scale of 300 hours (i.e., from pre-fire to post-fire steady state), the fire portion of the transient appears as a large but relatively short-lived spike in the boundary conditions that significantly affects only the outer shell, impact limiters, and the neutron shield, and to a lesser extent, the gamma shield, inner shell, and canister. The outer shell and neutron shield show a rapid temperature increase during the fire, but after the end of the fire immediately begin to rapidly cool down. Peak component temperatures for the HI-STAR 100 over the entire 300-hr transient fire simulation are summarized in Table 7.2.

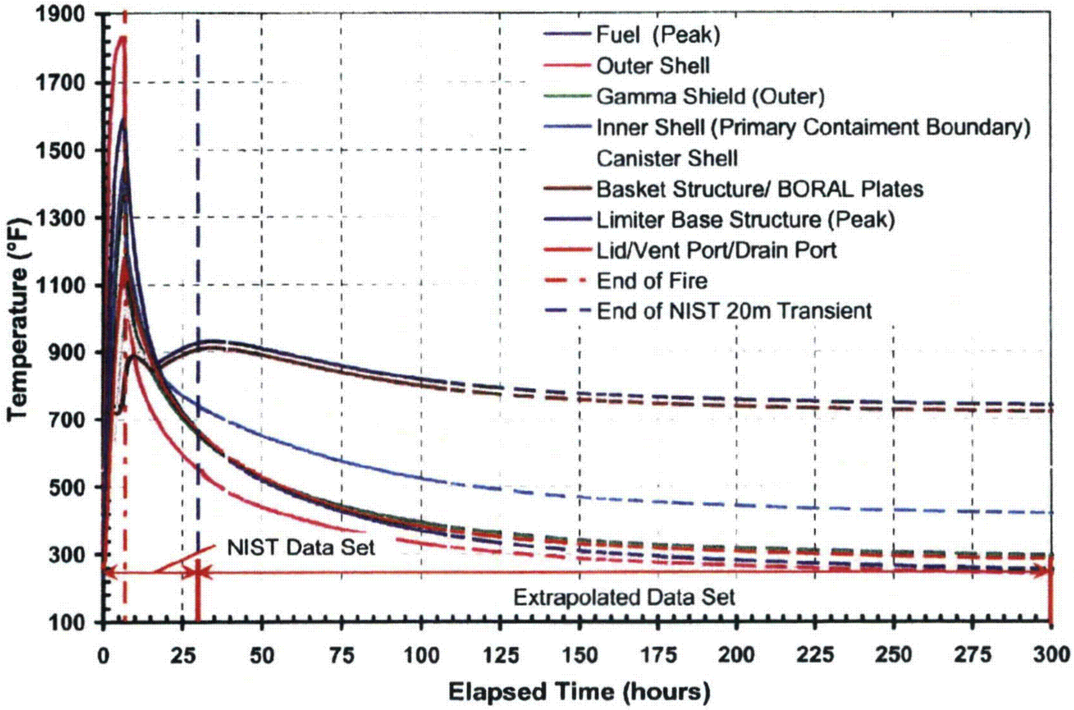


Figure 7.14. Maximum Temperature Histories for HI-STAR 100 Package Components During 300 hrs of the Transient

The temperature of the fuel and basket is largely unaffected by the heat input to the package from the fire. The increase in peak clad temperature and peak basket temperature is due almost exclusively to having essentially no heat removal from the package during the fire and for about an hour immediately afterward. After ambient temperatures drop enough to allow heat removal from the package, the rate of increase of the peak clad temperature begins to level off, and finally turns around at about 35 hours into the transient.

The TN-68 system displays a rapid peak cladding temperature increase during the interval from about 6 to 8 hours of the transient, followed by a much slower rate of increase until about 16 hours, at which point it begins to rise again toward its final peak value, reached at approximately 40 hours. The peak cladding temperature predicted for the HI-STAR 100 follows a similar pattern, but with a somewhat more dynamic response. After reaching a peak at approximately 10 hours, the peak cladding temperature actually decreases for a time, until about 15 hours, at which point it begins to rise toward its final peak value, reached at about 35 hours.

Table 7.2. HOLTEC HI-STAR 100 Peak Component Temperatures During Fire Transient

Component	Maximum Temperature (ANSYS) °F (°C)	Time (hours)
Fuel Cladding	930 (499)	35
MPC Basket	911 (488)	35
Boral	911 (488)	35
Canister Shell	1041 (560)	7.3
Inner Shell and Forgings	1447 (786)	6.8
Gamma Shield	1404 (762)	6.8
Package Skin	1831 (999)	6
Lid/Vent/Drain Port Seals	1181 (638)	6.8
Impact Limiter Skin	1826 (997)	6
Impact Limiter Structure	1591 (866)	6.8

The difference in response of the peak clad temperature in the two packages is due to three main factors. There are significant differences in construction and thickness of the finned neutron shield regions in the two package designs. There is about a 15% difference in the thermal inertia associated with the spent fuel assemblies in each package (the HI-STAR 100 contains 24 PWR fuel assemblies, compared to 68 BWR fuel assemblies within the TN-68 package), and the two packages have very different basket designs. In addition, the high thermal conductivity of the HI-STAR 100 aluminum honeycomb impact limiters aids in ramping up component temperatures faster in the ends of the package, compared to the effect of the redwood impact limiters on the TN-68 package.

The TN-68 does not utilize an internal canister to hold spent fuel. It relies instead on the cask shell structural integrity and seals to prevent the release of radioactive materials from the fuel compartment. The maximum predicted seal temperature, which is seen by the package lid seal, is 811°F (433°C), and occurs at the end of the fire. This is below the peak seal temperature predicted for the HI-STAR 100 and is primarily due to the relatively low conductivity of the redwood material used in the TN-68 impact limiter design, compared to the aluminum honeycomb in the HI-STAR 100 impact limiter design.

When comparing the heating trends associated with the HI-STAR 100 and the TN-68 (comparing results shown in Figure 7.14 and in Figure 7.8), it appears that the HI-STAR 100 generally heats up faster during the fire than the TN-68. However, this is mainly an artifact of the differences between the initial steady-state conditions in the two packages, different exterior packaging, and differences in their respective fuel loading. The HI-STAR 100 inner components enter the fire transient hotter by 100 °F to 200 °F (56 °C to

111 °C) than the corresponding components of the TN-68. These temperature differences are due to the redundant encapsulation provided by the MPC canister in the HI-STAR system, the number of fuel assemblies that the decay heat is distributed over (24 for the HI-STAR 100 versus 68 for the TN-68), and the level of shrouding of the package surface by the support device. The HI-STAR 100 is heavily shrouded by its support cradle; the TN-68 outer surface is essentially bare to ambient conditions.

In addition to these essentially incidental differences, there are some small differences in design that affect the rate of heat-up of the outer shells of the two packages. The TN-68 has a 50% thicker solid outer skin which distributes the heat from the fire transient circumferentially to cooler regions of the package more effectively than the thinner outer skin of the HI-STAR 100 package. The outer skin of the HI-STAR 100 package consists of relatively narrow welded metal strips, rather than a single steel sheet. The 0.19-inch (0.48 cm) fillet welds joining the metal strips (which were explicitly accounted for in the ANSYS model) present an additional barrier to circumferential heat flow in the HI-STAR 100 package outer shell. However, because both packages present a very large thermal mass to the fire and have very similar overall designs, they respond in essentially the same manner to the fire transient. The differences shown in these two sets of results consist mainly of minor time-shifts in the response to the imposed boundary conditions, and in general the behavior of the two sets of curves makes them almost indistinguishable.

7.3 NAC LWT Fire Transient Results

The ANSYS model of the NAC LWT package consists of a total of 50,673 standard computational elements and 12 superelements that are solved for each time step. Similar to the TN-68 and HI-STAR 100 models, this model yields a large amount of output that has been processed to characterize the package response. The following three subsections discuss the hypothetical package response to the fire transient conditions described in Section 6 in terms of the peak temperatures versus time for selected components.

7.3.1 NAC LWT During the Fire

Figure 7.15 shows the initial temperature response for the NAC LWT package and ISO container, as predicted with ANSYS, during the fire portion of the transient. The maximum temperature on the exterior surface of the ISO container surrounding the NAC LWT package increases rapidly to a peak temperature of 1592°F (867°C) at around 6 hours into the fire. This is 307 °F (170 °C) below the HI-STAR 100 external surface peak temperature, and 265 °F (147 °C) below that of the TN-68. This difference is due to the substantial view that the hottest portion of the ISO container has of cooler nearby surfaces. The top of the ISO container can exchange energy by thermal radiation with the package body and the interior surfaces of the sides and bottom of the ISO container. In contrast, the bare external surfaces of the HI-STAR 100 and the TN-68 see only the fire and the hot walls and ceiling of the tunnel.

The maximum temperature of the exterior surface of the NAC LWT package is 1525°F (829°C), only slightly lower than the peak temperature on the ISO container surface. The maximum temperature of the package inner shell material, which defines the primary containment boundary along with the bolted lid, shows a more gradual increase than the outer shell temperature, reaching a peak of 1261°F (683°C) at about 7 hours into the fire.

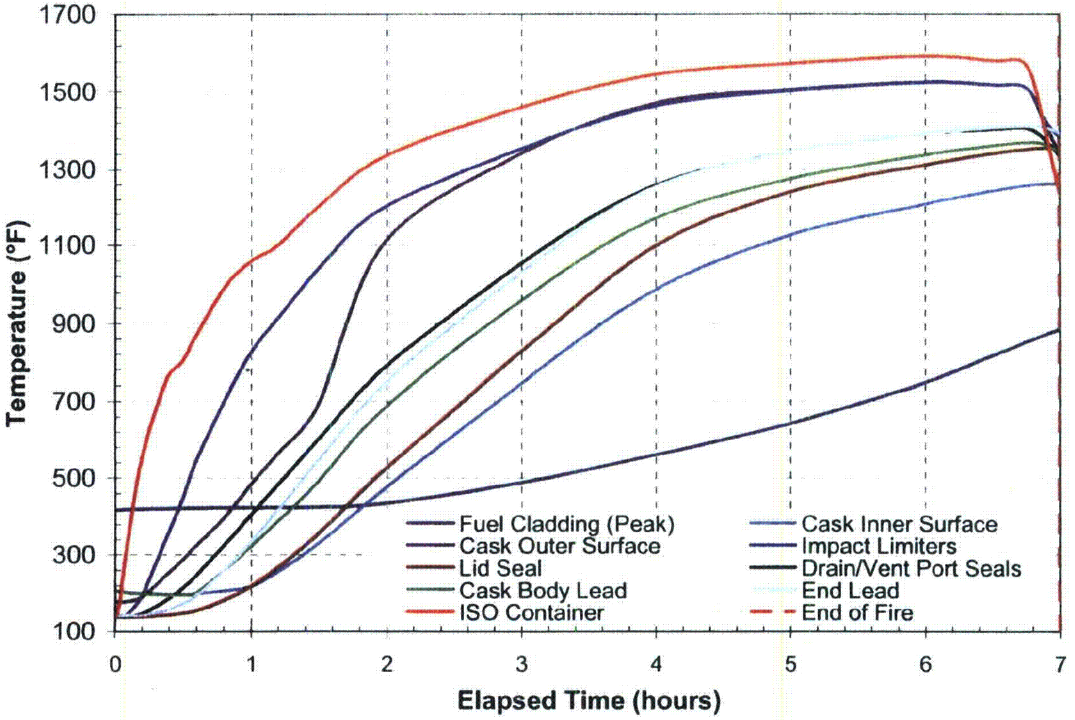


Figure 7.15. NAC LWT Component Maximum Temperature Histories During Fire Transient

Unlike the TN-68 and the HI-STAR 100, the internal components of the LWT package, particularly the fuel assembly, exhibit a noticeable thermal response during the fire. The peak fuel cladding temperature begins to rise at about two hours elapsed time, and the package structural components show a fairly rapid rise in temperature in the first hour of the fire. This occurs primarily because this package has considerably less thermal inertia than the two larger multi-assembly packages. The additional heat transfer paths available into the LWT package are also contributing factors, resulting from the fuel assembly being exposed within a cavity at each end of the package. As the inner shell surrounding the assembly ends heats up, radiation exchange within the cavities generates cladding temperatures at the ends of the fuel rods that are significantly higher than the temperatures at the center, as illustrated in Figure 7.16.

By the end of the fire (at approximately 7 hours), the predicted peak fuel cladding temperature, which occurs in the end region of the fuel, has reached 884°F (473°C) and is still rising. This value continues to increase for another three hours, but does not exceed the currently accepted short term temperature limit of 1058°F (570°C) for Zircaloy-clad spent nuclear fuel under accident conditions [23].

Other components of the package, in contrast to the peak cladding temperature, reach their peak temperature values at or very close to the end of the fire. This behavior closely follows the sudden decrease in the external thermal load on the package as the fire burns itself out. This can readily be seen in the peak temperatures reached on the gamma shield and the neutron shielding structures.

Figure 7.16. Lumped Fuel Assembly Temperature Distribution 7 hr into Transient

The gamma shielding is provided by a lead layer between the inner and outer shells as well as a lead billet in the welded base (i.e., the end opposite the package lid). The temperature of the lead in the package body reaches a maximum of 1369°F (743°C) at 6.75 hours elapsed time, and the temperature of the end billet peaks at the same time, at 1413°F (767°C). These temperatures are considerably greater than the established safe operating limit of 600°F (316°C) [11] for this material, and are significantly above the melting temperature of 622°F (328°C) for lead. High temperatures are sustained in these components long enough for the lead layer to entirely melt during the transient.

Figure 7.17 illustrates the thermal response of the lead shielding layer in the package body, showing the peak temperature in this component over time, and temperature histories of selected nodes within the lead region. Table 7.3 summarizes the process of melting and resolidification of the lead material in response to the fire transient. Melting of the lead begins at about 1.7 hrs, when the peak temperature in the end billet reaches the melting temperature of lead. The peak temperature in the package body shield is not far behind, reaching melting temperature about 9 minutes later.

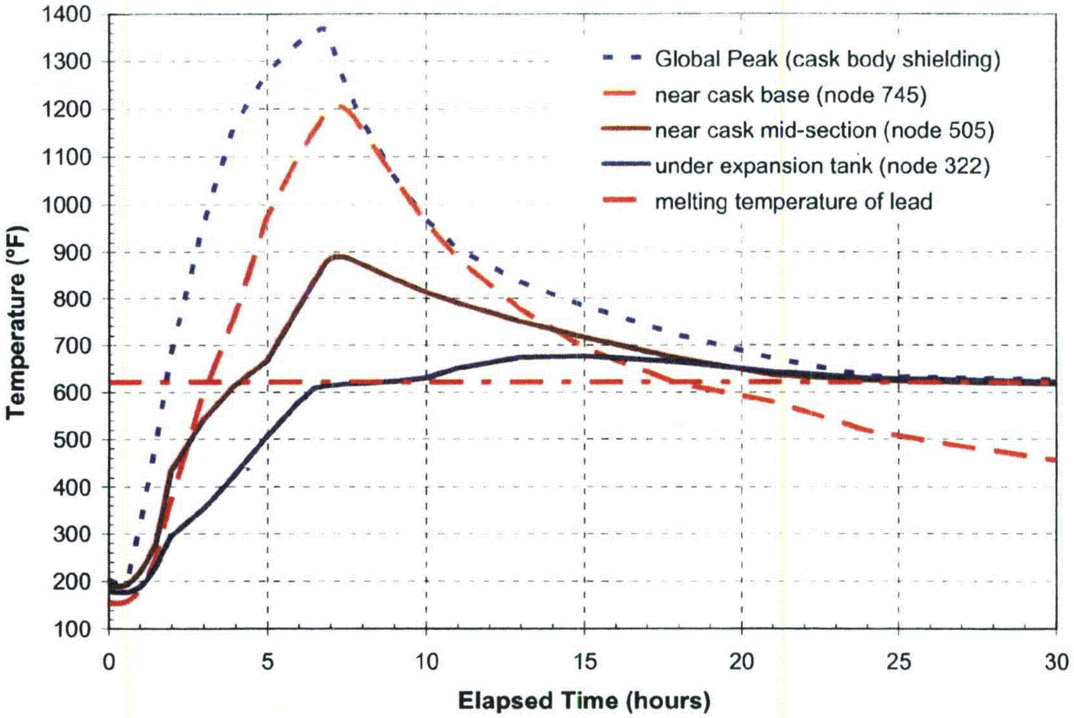


Figure 7.17. Temperature History of Selected Elements in Cask Body Lead in NAC LWT Package

Table 7.3. NAC LWT Lead Shielding Response to Fire Transient

Lead Shielding Thermal Response	Time (hours)
End billet lead begins to melt	1.69
package body shielding lead begins to melt	1.83
Melting within package body shielding near base	3.16
Melting within package body shielding near end of basket region	4.02
Melting within package body shielding near midsection	4.07-4.13
Melting within package body shielding under expansion tank	6.5
All lead shielding in molten state	9
End billet lead resolidified	16.05
Package body shielding lead begins to solidify	27
All lead shielding material resolidified	33.76

These initial melting peak temperatures occur on the very outer edge of these components, and considerably more time is required for interior temperatures in the lead region to reach melting temperature. As shown by the individual node temperature histories in Figure 7.17, at least another hour elapses before an interior node near the base of the package body shield reaches melting temperature, at about 3.16 hours into the transient. Most of another hour elapses before the middle section of the package body shielding reaches melting temperature, at about 4 hours into the transient.

The temperature history of an interior node near the package mid-section shows the effect of the phase change process in the change in slope as the temperature approaches the melting point. The energy absorbed in the phase change process (due to the latent heat of fusion, at 10.4 Btu/lbm (24.2 kJ/kg)) tends to slow the rate of temperature increase in the interior nodes of the region. A similar effect is seen in the temperature history of the selected node in the region under the expansion tank, although the temperature response of this node is also showing the thermal shielding effect of the expansion tank. The temperature rise is much slower at this location, and this element does not reach melting temperature until very close to the end of the fire, at about 6.5 hours elapsed time. An additional 2.5 hours is required for this portion of the cask body shielding to entirely melt.

The peak temperatures for the lead components show that the lead in the end billet begins to solidify at about 16 hours into the transient. It takes an additional 11 hours (about 27 hours elapsed time) before elements in the package body shield cool back down to the melting temperature, beginning a rather slow process of solidification. It takes another 6.8 hours (to about 33.76 hours elapsed time) before the peak lead temperature in the package body shielding drops below 622°F (328°C).

To maximize heat input to the package during the transient, the lead material was treated analytically in a manner that allowed the greatest possible heat transfer into the package from the fire. It was assumed that thermal expansion and expansion of the lead due to phase change would result in the lead entirely filling the cavity between the inner and outer steel shells of the package. As a result, there would be no gap resistance to heat flowing inward from the outer shell of the package. For the thermal analysis, possible slumping of the lead as a consequence of melting was conservatively ignored. (However, the potential for reduced gamma shielding as a result of the lead slumping is considered in Section 8.1.2.)

The response of the neutron shielding material was also treated in a manner to maximize heat input to the package. As described in Section 5.3, the temperatures of the nodes representing the main tank and overflow tank, both of which contain a 56% ethylene glycol and water mixture, were monitored for temperatures indicating rupture and evaporation throughout the transient solution. Similar to assumptions in the standard fire analysis included in the SAR [11], the liquid in the tank is expected to lose its shielding capability when the temperature exceeds its 350°F (177°C) boiling point. The NAC LWT is designed to attenuate neutron radiation to acceptable levels (see 10 CFR 71.51 [1]) following an accident without the assistance of the neutron shield material (as are the TN-68 and HI-STAR 100; see Section 7.1 and 7.2 above). However, the loss of the neutron shield affects the rate of heat transfer into and out of the package during and after the fire transient.

As a measure of conservatism, tank rupture was considered to occur only after the average ethylene glycol temperature for each tank exceeded 350°F (177°C). This assumption effectively delays rupture to a slightly later point in the transient than might be expected, retaining the higher heat transfer rate through the liquid for a longer period before replacing it with lower conductivity air, and thereby maximizing heat input into the package. The model predicted that the inner neutron shield tank and the outer expansion tank would rupture at ~1.5 hours. Following rupture, the effective conductivity of the tank was significantly decreased, due to the ethylene glycol volume being expelled and replaced with air. As a further conservatism, the energy absorbed in the phase change, due to the latent heat of vaporization for the ethylene glycol and water mixture, was not subtracted from the heat input to the package.

7.3.2 NAC LWT Short-Term Post-Fire Response

Figure 7.18 shows the peak temperatures predicted for various components of the package during the first 30 hours of the ANSYS transient simulation based on the NIST fire simulation results. The cladding peak and average temperatures continue to rise after the fire, just as in the analyses for the TN-68 and HI-STAR 100 packages, and for much the same reason. The ambient conditions in the tunnel, immediately following the fire, severely retard the rate at which the fuel decay heat can be removed from the package.

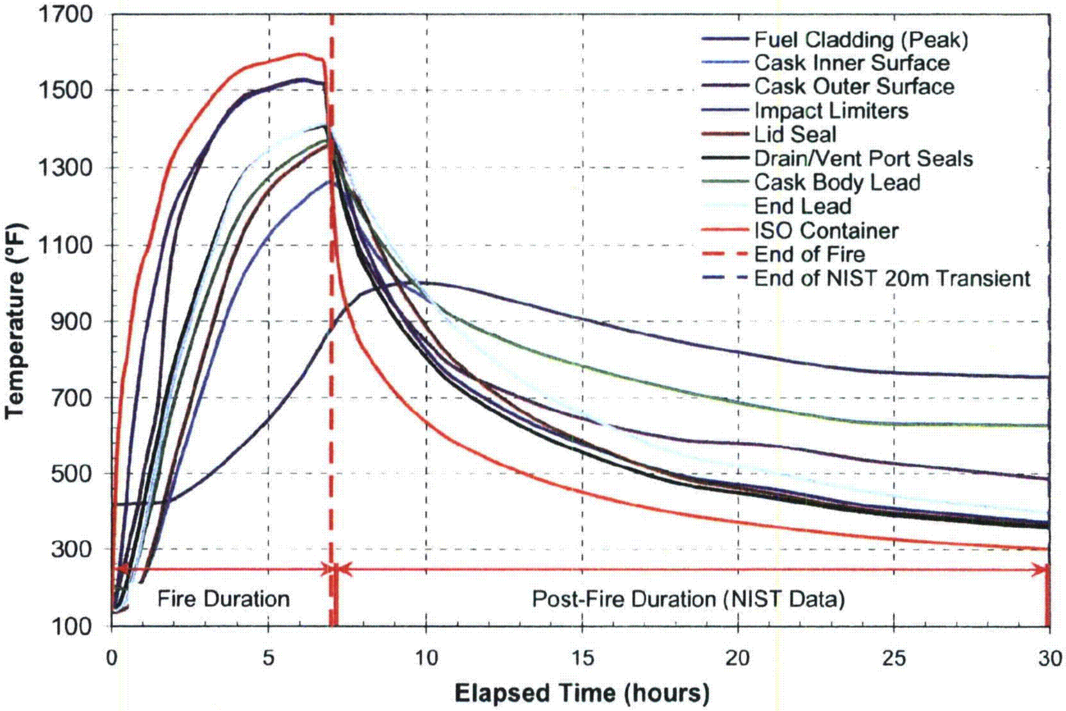


Figure 7.18. NAC LWT Package Component Maximum Temperature Histories for First 30 hours of Fire Transient

Figure 7.18 shows that the peak temperatures for all package components begin to decrease shortly after the end of the fire. The peak cladding temperature reaches its maximum value of 1001°F (589°C) at 10 hours. This is 57 °F (32 °C) below the short term limit of 1058°F (570°C), and 381 °F (212 °C) below the temperature at which Zircaloy fuel rods actually fail by burst rupture, which is approximately 1382°F

(750°C) [25]. (The maximum temperature for the basket reaches its peak of 958°F (512°C) at about 8 hours into the transient, but this temperature curve is omitted from Figure 7.18 for clarity.)

Once the fire is over, however, the predicted peak temperatures on outboard components (i.e., the ISO container and package outer surface) begin to drop rapidly in response to the rapid decrease in the boundary temperatures, as illustrated in Figure 7.19. This figure shows the outer shell surface temperature predicted with ANSYS, compared to the tunnel ceiling temperature and the temperature of the air above the ISO container, derived from the NIST calculations used as boundary conditions.

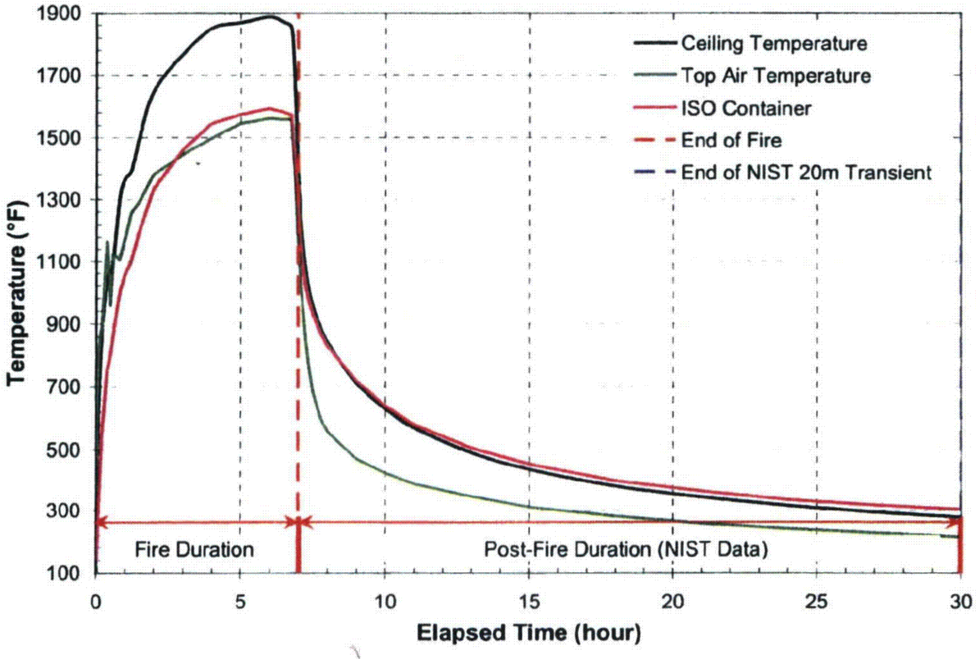


Figure 7.19. Maximum ISO Container Surface Temperature History Compared with NIST Boundary Condition Temperatures

As a result of the low thermal inertia of this package, peak temperatures in the various internal components occur within 2-3 hours of the fire being extinguished, rather than 35 or 40 hours later, as in the TN-68 and HI-STAR 100, respectively. Because of the heating of the ends of the fuel rods due to thermal radiation as a result of the fire, the *average* fuel temperature gradually increases to a maximum of 977°F (470°C). This peak is reached at 9 hours elapsed time, as shown in Figure 7.20.

The maximum temperature histories of the seals in the drain/vent ports and the lid are shown for the first 30 hours in Figure 7.21. (The calculated values were gathered by querying nodes at the seals' locations, since the seals were not explicitly represented in the model.) The drain and vent ports are sealed with Teflon O-rings. The bolted lid is sealed by both metallic and Teflon O-ring seals. The drain and vent port seals reach a maximum temperature of 1407°F (764°C), and the lid seal reaches 1356°F (735°C) at the end of the fire.

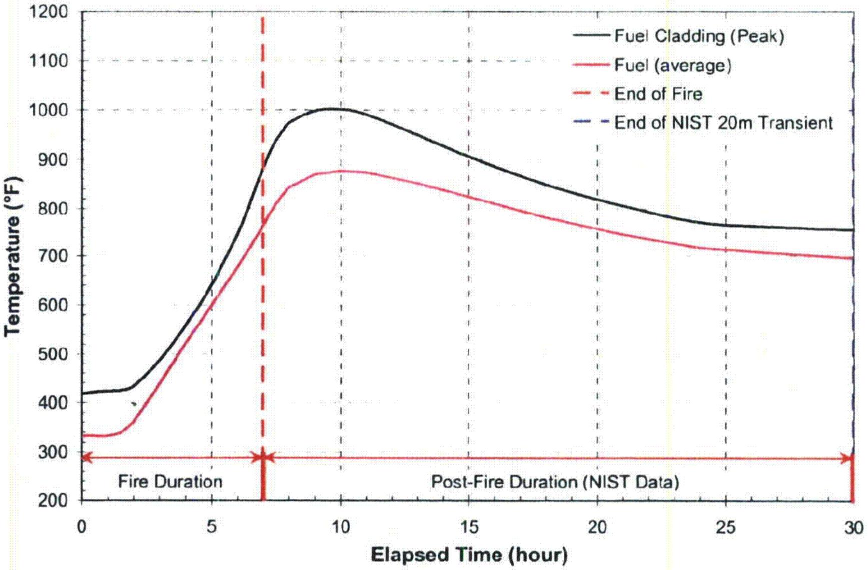


Figure 7.20. Peak and Average Fuel Cladding Temperature Histories for NAC LWT Package During First 30 hr of Fire Transient

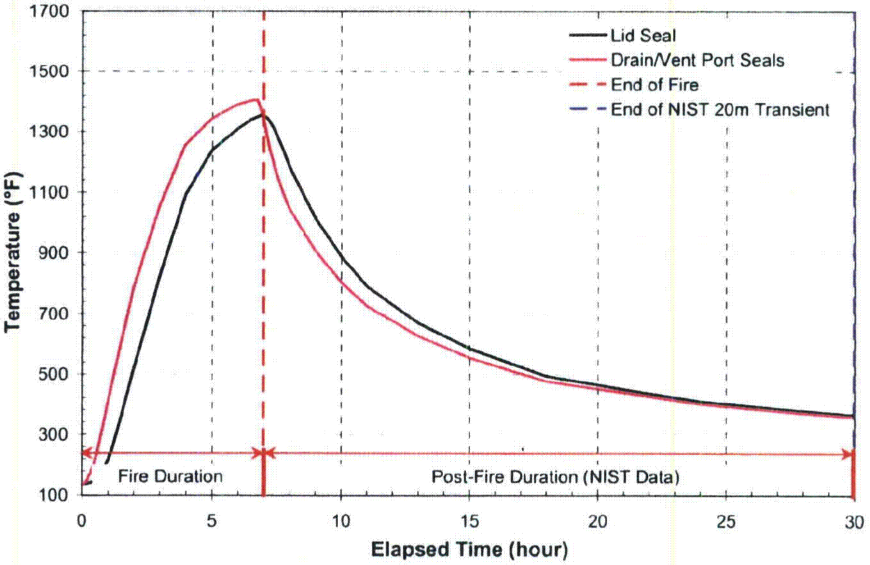


Figure 7.21. Maximum Seal Temperature Histories for Drain/Vent Ports and Package Lid During First 30 hr of Fire Transient

These materials then gradually cool as the transient proceeds into the post-fire cool down. The extreme rise in temperature is due to the low thermal inertia associated with the LWT package and the close proximity of the seals to exterior surfaces subject to thermal radiation from the thin ISO container, which is in turn subject to thermal radiation and convection heat input from the tunnel environment. The predicted seal temperatures are far greater than the maximum continuous-use seal temperature limits of 735°F (391°C) for the Teflon seals and 800°F (427°C) for the metallic seals.

7.3.3 NAC LWT Long-Term Post-Fire Response

As with the TN-68 and HI-STAR 100 analyses, the temperatures predicted in the NIST analysis were extrapolated from 30 hours to 300 hours using a power function in order to realistically model cool down of the tunnel environment. As discussed previously, this conservative approach is equivalent to assuming that the package will be left in the tunnel for nearly two weeks, without any emergency responder intervention. The same conservative assumptions used in the analysis of the TN-68 and HI-STAR 100 were used to define the convection heat transfer boundary on the NAC LWT package. A purely forced convection heat transfer regime was assumed for the first 30 hours of the simulation, then a purely free convection regime was assumed for the remainder of the calculation ($t \geq 30$ hours). (The extrapolated boundary conditions are shown in Figures 6.5 and 6.6 for the tunnel air and surface temperatures, respectively.) Peak component temperatures for the NAC LWT over the entire transient fire simulation are reported in Table 7.4.

Table 7.4. NAC LWT Peak Component Temperatures During Fire Transient

Component	Maximum Temperature (ANSYS) °F (°C)	Time (hours)
Fuel Cladding	1001 (539)	10
Aluminum PWR Insert	958 (515)	8
Inner Shell	1261 (683)	6.9
Lead Gamma Shield:		
Package body	1369 (743)	6.75
End billet	1413 (767)	6.75
Outer Shell	1525 (829)	6
Neutron Shield:		
Shield tank	1483 (806)	6
Expansion tank	1524 (829)	6
Lid Seal	1356 (732)	6.9
Drain/Vent Ports	1410 (766)	6.75
Impact Limiters	1521 (827)	6
ISO Container	1592 (867)	6

Figure 7.22 shows the temperature response of the various components of the package for the long term transient calculation to 300 hours. The maximum temperatures were reached within a short time after the end of the fire, and the LWT at 100 hours is very close to its new steady-state condition. This behavior is consistent with its lower thermal inertia, in comparison to the larger multi-assembly packages.

Temperature distributions within the package for the final steady state will be slightly different than the original, due to the dissipation of the liquid neutron shield, changes in the surface emissivities because of the fire, and tunnel ambient conditions that differ from the hot-normal conditions assumed for the pre-fire steady state (i.e., lower ambient temperature and the absence of solar insolation).

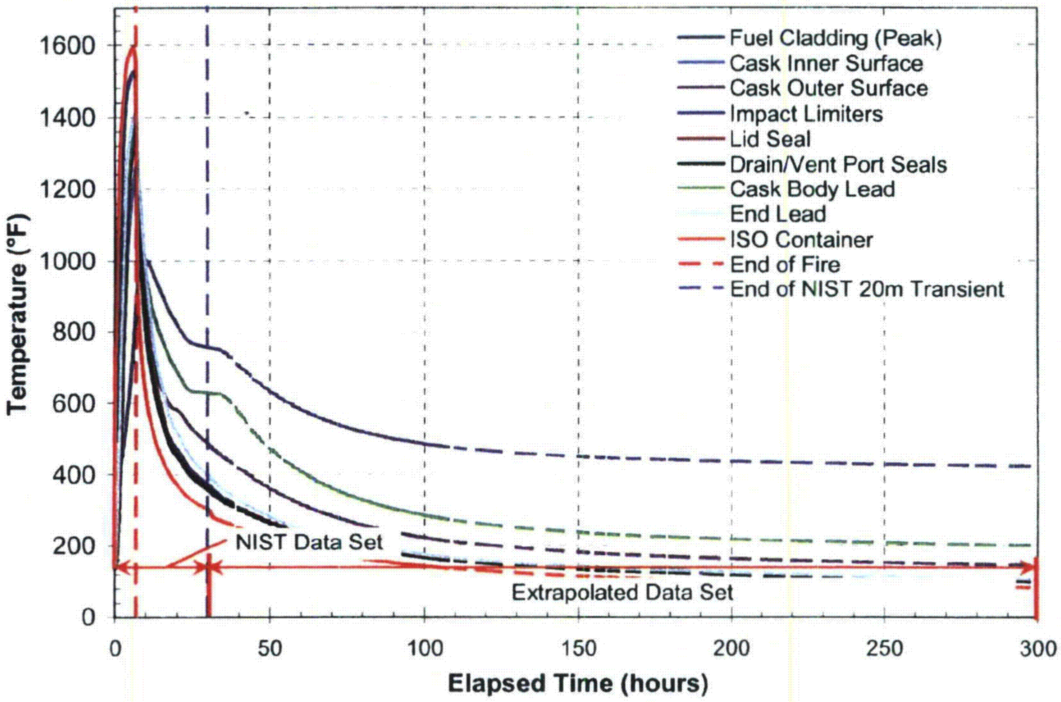


Figure 7.22. NAC LWT Package Component Maximum Temperature Histories During 300 hr Transient

8 POTENTIAL CONSEQUENCES

The potential consequences of a severe accident involving an SNF transportation package fall into two general categories: 1) the possibility of a direct radioactive dose to a member of the public due to a loss of either the neutron or gamma radiation shielding of the package, and 2) the potential release of radioactive material from the package due to a compromise of the containment boundary. Section 8.1 discusses potential loss of shielding events. Section 8.2 evaluates potential releases from each of the evaluated package designs. Section 8.3 provides a summary of the potential consequences of involving an SNF package in the Baltimore tunnel fire scenario.

8.1 Potential Consequences of Loss of Shielding

USNRC Staff evaluated the potential for increased neutron and gamma radiation dose rates from each of the three transportation packages (TN-68, HI-STAR 100, and NAC LWT) as a result of exposure to the Baltimore tunnel fire scenario. The analysis indicates that the regulatory dose rate limits for accident conditions, specified in 10 CFR 71.51, would not be exceeded by any of these packages in this fire scenario, even though all three packages would be expected to lose neutron shielding and the NAC LWT could possibly experience some loss of gamma shielding, as well. Section 8.1.1 describes the consequences of loss of the neutron shielding. Section 8.1.2 discusses the potential effects of loss of gamma shielding, with particular emphasis on the potential consequences of slump of lead shielding material in the NAC LWT.

8.1.1 Neutron Shielding

Neutron shielding in SNF transportation packages is typically provided by materials that have relatively low melting temperatures (such as hydrocarbon resins or polymers), or are liquid at ambient conditions (such as water or mixtures of water and glycol). These materials are not expected to survive the design-basis accidents specified in 10 CFR 71, and the analyses included in the SAR for an SNF transportation package typically assume loss of the neutron shield in all accident scenarios.

The packages are designed to meet the regulatory radiation dose limits for all conditions of transport, including hypothetical accident conditions. (Refer to the respective SARs [9, 10, 11] of these three packages for details on the analyses supporting this design constraint). The severe conditions of the Baltimore tunnel fire scenario could do no more damage to the neutron shields of these SNF packages than is assumed in the required regulatory fire analyses. All three of the packages considered in this evaluation can meet the regulatory limits, even when their neutron shielding has been destroyed by fire.

8.1.2 Gamma Shielding

The effectiveness of gamma shielding materials can be reduced or lost in one of two ways:.1) the gamma shield material could be dislocated due to thermal effects, or 2) the gamma shield material could be lost from a package if the package's outer wall was breached and the shield material subsequently melted. Reduction in the effectiveness of gamma shielding due to thermal effects such as thermal expansion or contraction, or slumping, could result in shielding material that may not be in the proper position to provide the degree of shielding required,. For loss of shielding material due to a breach, molten material might escape through the breach, reducing the amount of material available to provide the required gamma shielding.

The TN-68 and the HI-STAR 100 packages would not experience a reduction in or loss of gamma shielding material effectiveness due to either of the two previously described mechanisms in this fire scenario. The gamma shielding in these packages is composed of multiple layers of carbon steel and stainless steel. Both types of steel have extremely high melting temperatures, in the range 2500-2800°F (1371-1538°C), which is far above the peak temperature attained on any package component in this fire scenario.

The possibility of a decrease in the effectiveness of the gamma shielding for the NAC LWT package, however, cannot be ruled out. In the severe conditions of the Baltimore tunnel fire scenario, the lead comprising the gamma shield of the NAC LWT is expected to begin to melt when the local temperature of the material reaches 622°F (328°C). This process of local phase change in the material absorbs additional energy from the fire due to the latent heat of fusion, which for lead is 10.4 Btu/lbm (24.2 kJ/kg). The gamma shielding in the package body consists of approximately 24,441 lbm (11,089 kg) of lead, and will absorb about 254,000 Btu (2.7 x (10^8) J or 74.5 kW-hr) in the process of melting.

This process takes considerable time in this fire scenario, beginning at approximately 2 hours into the transient and extending beyond 9 hours elapsed time, and absorbs a significant amount of energy. In a short-duration fire, the thermal input from the fire may not be large enough to cause all of the lead to melt. In the Baltimore tunnel fire scenario, however, this analysis predicts that the peak temperature of the gamma shielding material would be above the melting point of lead for approximately 25 hours, and the entire gamma shield becomes molten within the annular cavity in the steel shell and within the package base.

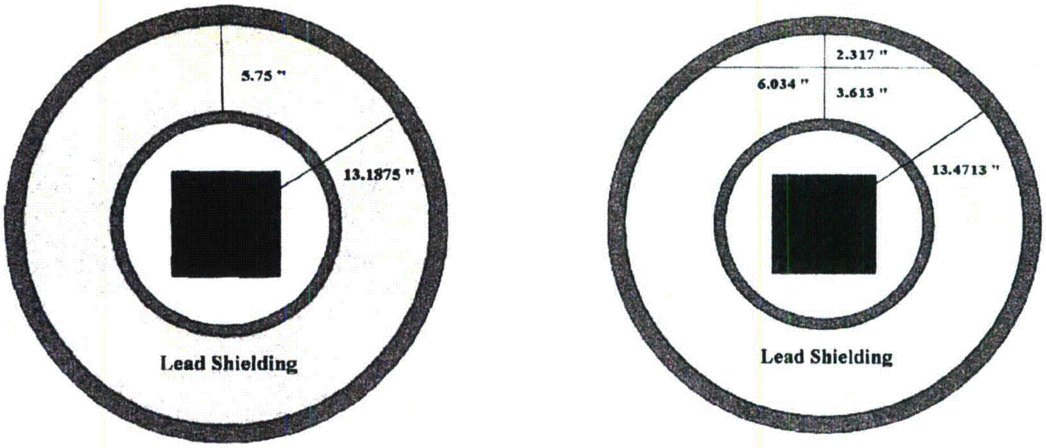
Melting of the lead does not necessarily imply complete loss of gamma shielding, however. For a significant amount of shielding to be lost, a puncture must penetrate the 1.2-inch thick stainless steel package shell, to allow molten lead to flow out of the package. For the derailment in the Howard Street tunnel, the impact forces involved were not of a sufficient magnitude to result in a breach of the package wall. Therefore, there is no expectation of loss of gamma shielding due to loss of molten lead from the package in this scenario.

In the absence of a breach of the package outer shell, the lead would be completely retained within the steel annulus of the package shell and within the package base. Even when molten from the effects of the fire, the lead would continue to act as a gamma shield. Some reduction of shielding effectiveness could occur, however, due to slumping of the lead within the confines of the annulus as it shrinks upon resolidification. This could result in the formation of voids between the package wall and the lead shielding material.

The NAC LWT package is designed to accommodate some amount of void space between the steel and the lead, due to fabrication constraints. The lead shielding is poured in molten form into the steel annulus. The steel shell is heated to 550-650°F (288-343°C) during this process, and then allowed to cool to ambient temperature as the lead solidifies. Analyses presented in the SAR [11] conservatively assume a 0.35-inch (0.1374-cm) gap between the outer diameter of the lead and the inner surface of the package outer shell, due to lead shrinkage upon solidification and differential thermal expansion and contraction between the lead and the steel package body. Lead slump analyses in the SAR show that the package maintains gamma shielding when the lead slumps to fill this gap in the hypothetical drop accidents. The SAR analyses indicate dose rates below the regulatory limit of 1000 mrem/hr (10 mSv/hr) for the slumped condition.

In the Baltimore tunnel fire scenario, the slumping of the lead could be more severe than predicted for the analyses in the SAR, because of a larger increase in the volume of the annulus containing the lead. Thermal expansion of the package outer shell, due to the high temperatures reached by the steel, and increased hoop stress on the steel due to expansion of the molten lead, could result in a significant increase in the volume of the annulus between the inner and outer steel shells of the package. A conservative analysis suggests that the increase in the size of the cavity containing the lead shielding could be as great as 5%. When the molten lead resolidifies, it will not quite fill the enlarged cavity, resulting in extra void space between the lead and the steel shell of the package.

Calculations were performed by NRC staff to determine the size of this void space, assuming that gravitational settling would result in the entire void space occupying a continuous volume within the annular cavity containing the lead shielding. For the horizontal orientation of the package assumed in this fire scenario, this results in a void volume extending the full length of the upper edge of the annulus, as illustrated in the cross-section diagrams in Figure 8.1. This figure shows a schematic representation of the configuration of the lead comprising the NAC LWT gamma shield before and after the fire, including conservative estimates of the change in dimensions of the cavity containing the lead shielding material.



before lead melting **after lead melting**

Figure 8.1. Lead Shielding Configuration in the NAC LWT Before and After Lead Melting

The analysis shows that for the horizontal orientation, the thickness of the lead in the upper region of the annulus could be reduced by up to about 2.3 inches. Because of the design of the gamma shielding, which extends at least 15.5 inches (39 cm) beyond the active fuel region and overhangs the cask inner cavity by 7.5 inches (19 cm) at either end, the horizontal orientation of the package is the most adverse orientation for lead thinning as a result of melting and relocation due to gravitational settling. If the cask were vertical, the total height of the void volume would be only 10.5 inches (27 cm), resulting in reduced shielding for only about 3 inches at the top of the cask inner cavity in a region of relatively low activity, while the shielding remained at full thickness in the active fuel region of the assembly. Geometric considerations show that if the cask were merely tilted at some angle to the vertical, rather than being fully horizontal, a large portion of the void volume would be taken up in the upper end of the annular cavity containing the lead shielding material. As a result, there would be less thinning of the shielding thickness, and the thinning would extend over a smaller area. There would therefore be a lesser potential radiation dose resulting from reduction of shielding.

An estimate of the potential radiation dose resulting from the maximum possible localized thinning of the lead shielding with the cask in a horizontal orientation is provided in Table 8.1. The total potential dose rates in Table 8.1 are due to radiation only, and include the increased neutron dose rate due to the loss of the liquid neutron shield (as documented in the SAR [11] for the NAC LWT), plus the increased ionizing radiation dose rate due to the thinning of the gamma shield. Thinning of the gamma shielding would not result in any release of radioactive material from inside the package. (See Section 8.2 for discussion of potential dose rates due to release of radioactive material.) The safety issue due to lead slumping is strictly a matter of the possible dose to first responders from a package involved in a severe fire accident scenario. However, as shown in Table 8.1, this dose rate does not exceed the accident limit of 1000 mrem/hr (10 mSv/hr) at one meter from the package surface, as specified in 10 CFR 71 and 49 CFR 173.

Table 8.1. Potential Dose Estimate from NAC LWT with Reduced Shielding

Location	Intact Package mrem/hr (mSv/hr)	After lead melt mrem/hr (mSv/hr)	Regulatory Limit * mrem/hr (mSv/hr)
Surface	**Neutron:** 7.09 (0.0709)	**Neutron:** 177.13 (1.773)	
	Gamma: 48.62 (0.4862)	**Gamma:** 1216 (12.16)	
	Total: 55.71 (0.5571)	**Total:** 1393 (13.93)	
1 m from surface	**Total:** 14.99 (0.1499)	**Neutron:** 50.93 (0.5093)	
		Gamma: 331.3 (3.313)	1000 (10)
		Total: 382.2 (3.822)	

*from 49CFR173 and 10 CFR 71.51(a)(2)

This conclusion is consistent with the results of analyses presented in the package SAR [11] for reduction of gamma shielding due to lead slumping as a consequence of the hypothetical drop accidents specified in 10 CFR 71. In the analyses presented in the SAR for these hypothetical accidents, the maximum dose rates do not exceed the design limits specified in 10 CFR 71 and 49 CFR 173, and are well below the limit of 1000 mrem/hr (10 mSv/hr) at one meter from the package surface.

8.2 Potential Release Issues

USNRC Staff evaluated the potential for a release of radioactive material from each of the three transportation packages (HI-STAR 100, TN-68 and NAC LWT) analyzed for the Baltimore tunnel fire scenario. The analysis indicates that there would be no release expected from the HI-STAR 100 package. However, the possibility of a small release cannot be entirely ruled out for either the TN-68 or NAC LWT packages, because temperatures during the fire or cool down period exceed the manufacturer's recommended service temperature limits for the package lid seals and seals on the vent and drain ports.

The thermal analyses show that the potential release would not involve a release of spent fuel or fission products, but could possibly result from CRUD detaching from the fuel rods. Any potential release from either the TN-68 or NAC LWT package would be small—less than an A_2 quantity. An A_2 quantity[18] is defined in 49CFR173.403 as the maximum activity of Class 7 (radioactive) material permitted in a Type A package. Type A packages carry such small amounts of radioactive material that an accident resistant package is not required. This is because an A_2 quantity of radioactive material would not be expected to result in a significant radiological hazard to first responders even if it were released from the package due to a transportation accident. Type B packages (which include SNF transportation packages) can carry more than an A_2 quantity of radioactive material, but must retain the integrity of containment and shielding under normal conditions of transport, as required by DOT regulations in 49 CFR part 173. Type B packages must also be designed such that if one were subjected to the hypothetical accident conditions specified in 10 CFR part 71 [1], it would release less than an A_2 quantity/week.

Staff performed an analysis to determine the magnitude of any potential release, assuming the packages contained spent fuel that was 5 years old. Because it was determined by the thermal analyses conducted for each package that the fuel cladding for the fuel assemblies remains intact, it is not expected that any radioactive material would be released from inside the fuel rods. This limits any release from the package to CRUD particles that may detach from individual fuel rods.

Rather than addressing all radionuclides that could be contained in such CRUD particles, (see Reference [26], Table I-7), the radionuclide of the greatest concern was used as the basis of the release calculation. For shipments consisting of fuel that is 5 years old or older, Co^{60} is the most important radionuclide to be considered. For fuel that is less than 5 years old, other short-lived isotopes, such as Mn^{54} and Co^{58} should be considered as well [26]. For PWR fuel cooled for 5 years, the total activity decreases to 3% of that at discharge, while after 13 years, the activity drops to 1% of that at discharge. Co^{60} accounts for 92% of the activity at 5 years and 99% at 8 years. For BWR fuel, the total activity decreases to 31% of that at fuel discharge after 5 years and 1% after 30 years. Co^{60} accounts for 98% of the activity at 5 years (see page I-50, Ref [26]).

A discussion of seal performance and leakage pathways is provided in Section 8.2.1. The results of the release analysis for the HI-STAR 100 package are provided in Section 8.2.2, and the results for the TN-68

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[18] The actual amount of a particular material that constitutes an A_2 quantity depends on the radiological properties of the material. Appendix A of 10CFR71 defines the A_2 quantities for a large number of different materials in Table A-1, and specifies methods for calculating the appropriate value for any material not listed in the table.

are provided in Section 8.2.3. Results for the NAC LWT are provided in Section 8.2.4. Additional analyses are presented in Section 8.2.5, investigating the potential for releases from the HI-STAR 100 and NAC LWT when these packages are transporting failed fuel.

8.2.1 Seal Performance and Potential Leak Paths

A simple "pass/fail" criterion is used for evaluating seal performance in this study. If the manufacturer's maximum recommended service temperature was exceeded at any time during the transient on any portion of the sealing surfaces, the seal was assumed to fail. Seal failure is defined as the inability of a seal material to maintain a seal against the internal pressure of the package cavity. This constitutes a conservative criterion, because exceeding the manufacturer's service temperature limits for the seal material used in a spent fuel package lid or vent and drain port seal is not a direct indicator of seal failure, and does not necessarily mean that a release of radioactive material would occur from the package. The service temperature limits for seals are the temperature to which the manufacturer is willing to guarantee the seal's extended performance. Exceeding these temperatures does not necessarily mean that the seal will fail immediately, although it does suggest that there is a potential for seal failure to occur, due to the eventual degradation of the seal material. This could lead to the failure of the seal to hold against the internal pressure of the package cavity, thereby creating conditions that could lead to a release.

Similarly, failure of a seal does not remove all barriers to release of material from within the package. Even without the intact seals, potential releases from a package would be limited by the narrow, convoluted flow paths of the drain and vent ports, and by the tight clearances of the close metal-to-metal contact between the lid and package body. This close contact is maintained by the pre-load created by the initial torque on the lid bolts, and does not depend on the presence of the lid seals.

The exact temperature at which a particular seal will fail and the particular mechanism of that failure is not known *a priori*, because most seal manufacturers have not tested their seals to failure at higher temperatures. However, the point at which seal failure would actually occur is irrelevant to this study. Complete and total failure of the seal materials was assumed if the manufacturer's maximum recommended service temperature was exceeded at any time during the transient on any portion of the sealing surfaces. No credit is taken in the release calculation for the presence of any seals. This is considered to be a highly conservative approach.

8.2.2 Potential Release from the HI-STAR 100 Package

The thermal analysis shows that the HI-STAR 100 package design would maintain three important barriers throughout the fire and subsequent cool down period, which would prevent the release of radioactive materials. The welded inner canister remains intact and leak-tight, preventing any release from the fuel rods themselves or as a result of CRUD detaching from the fuel rods. The temperature of the fuel cladding is predicted to peak at about 930°F (499°C), well below the short-term temperature limit of 1058°F (570°C) for Zircaloy cladding, and significantly below its projected burst temperature of 1382°F (750°C). This would prevent the release of fission products from the fuel rods. The maximum temperature of 1181°F (638°C) predicted for the package's metallic O-rings is below their rated continuous-use service temperature of 1200°F (649°C). Thus, the O-rings would not be expected to significantly degrade.

8.2.3 Potential Release from the TN-68 Package

The thermal analysis for the TN-68 package shows that during the Baltimore tunnel fire scenario, this package design would maintain the integrity of the fuel cladding, which is the single most important barrier to prevent the release of radioactive materials. At approximately 40 hours elapsed time, the temperature of the fuel cladding would peak at about 845°F (452°C), well below the short-term temperature limit of 1058°F (570°C) for Zircaloy cladding and significantly below its projected burst temperature of 1382°F (750°C). This would prevent the release of fission products from the fuel rods. However, the metallic helicoflex seals used on the TN-68 lid and the vent and drain ports reach a maximum temperature of 811°F (433°C) by the end of the fire (at 7 hours elapsed time). This exceeds the seals' rated service temperature of 536°F (280°C) by 275 °F (153 °C).

Because the predicted temperatures exceed the long-term service temperature of the seals, the seals are assumed to fail, and there is a potential for release of radioactive CRUD particles from the package. The amount of releasable CRUD in the TN-68 package was estimated using data developed by Sandia National Laboratory for analysis of CRUD contribution to shipping package containment requirements [26]. The calculation was based on package contents consisting of 68 BWR fuel assemblies, each assembly containing 49 fuel rods. An estimate of the maximum "spot" CRUD activity shows that for 90% of BWR spent fuel rods the maximum activity is 300μCi/cm^2 or less [26, Table I-17]. The ratio of the peak to average concentration on the rod surface (i.e., the maximum "spot" CRUD activity over the average value) varies by a factor of two for BWR fuel rods [26, Table I-17].

The CRUD activity estimates [26] are based on newly discharged spent nuclear fuel. The CRUD activity is expected to decay by a factor of one-half for five-year-cooled fuel, based on the decay rate for Co^{60}. This proves to be a good approximation, because 98% of the activity for five-year-cooled BWR fuel comes from Co^{60}. Based on this data, the average CRUD activity for a BWR rod with a surface area of 1600 cm^2 is about 0.12 Ci for five-year cooled fuel. The average CRUD activity for a typical 7 x 7 BWR assembly is about 5.9 Ci.

The amount of CRUD that might flake or spall from the surface of a BWR rod due to thermal stresses induced by temperature change in the fuel rods is estimated to be a maximum of 15% [26, Table I-10]. The major driving force for material release results from the increased gas pressure inside the package, due to increases in internal temperature. The temperature change in the package is bounded by the difference between the maximum gas temperature predicted during the fire transient and the fill gas temperature at the time the package is loaded. For this analysis, the loading temperature is defined as 100°F (38°C), based on the temperature reported in the SAR [9]. The maximum fill gas temperature is assumed to be the maximum inner shell temperature, predicted during the transient, of 857 °F (458 °C). This yields a conservative estimate of the temperature change.

A deposition factor of 0.90 was used to account for the settling and deposition of CRUD particles on package surfaces and fuel assemblies. The deposition factor was developed as part of NRC'ssecurity assessments for spent nuclear fuel transport and storage packages, and is based on an analysis of the gravitational settling of small particles. The value of 0.90 is conservative because it does not consider the effects of particle conglomeration and plugging. It is also consistent with the values used in other studies [25]. The major assumptions used to estimate the potential CRUD release are given in Table 8.2.

Table 8.2. Assumptions Used for Release Estimate for TN-68 Package

Parameter	Assumed value
Number of Assemblies in TN-68 Package	68 BWR
Rods per Assembly	49
Maximum "spot" CRUD Activity on Fuel Rod	300μCi/cm^2
Peak to axial average variation	2
CRUD decay factor (5 yr; based on Co^{60})	0.5
Average surface area per rod	1600 cm^2
Average CRUD Activity on BWR Fuel Rod (5 yr cooled)	0.12 Ci
Average CRUD Activity on BWR Assembly (5 yr cooled)	5.9 Ci
Fraction of CRUD released due to heating	0.15
Deposition Factor	0.90

To estimate the potential release from the TN-68 package, a methodology similar to that developed by Sandia National Laboratory was used (see NUREG/CR-6672 [25]). This methodology was developed for evaluation of the generic risks associated with the transport of spent fuel by truck and rail from commercial power plants to potential interim storage and disposal sites.

The potential release from the TN-68 package can be estimated by adapting the equation developed in NUREG/CR-6672 ([25]) to estimate the releases from a severe fire accident. The estimated release is given by the relationship

$$R = C_t S(1 - D)\left(1 - \frac{T_i}{T_p}\right)$$

where R = release (curies)
C_t = amount of CRUD on fuel assemblies (curies)
S = fraction of CRUD released due to heating
D = deposition factor
T_p = peak internal temperature (°R)
T_i = initial internal temperature (°R)

Table 8.3 shows the results obtained when this equation is applied using the parameter values from Table 8.2 and the temperatures predicted for the TN-68 package in this accident scenario.

Table 8.3. Potential Release Estimate for TN-68 Package

Initial temperature °F (°R)	Peak temperature °F (°R)	Potential release (curies)
100 (560)	857 (1307)	3.4

The potential CRUD release from the TN-68 package, based on five-year cooled fuel, is estimated to be approximately 3.4 curies of Co^{60}. Since the A_2 value for Co^{60} is 11 curies, the potential release is about 0.3 of an A_2 quantity (see Section 8.2).

8.2.4 Potential Release from the NAC LWT Package

The thermal analysis for the NAC LWT package shows that this package design would also maintain the integrity of the fuel cladding during the Baltimore tunnel fire scenario, and thus would maintain the single most important barrier to prevent the release of radioactive materials. The peak temperature of the fuel cladding is conservatively predicted to reach 1001°F (539°C), a temperature that is below the short-term temperature limit of 1058°F (570°C) for Zircaloy cladding, and well below its projected burst temperature of 1382°F (750°C). This peak temperature occurs at approximately 10 hours after the start of the fire (i.e., after the 7-hour fire duration, about 3 hours into the cool down period).

At about 6.9 hours elapsed time, the temperature predicted in the region of the Teflon and metallic helicoflex seals used on the NAC LWT lid reaches a maximum value of 1356°F (735°C). This value exceeds the continuous-use rated service temperature limit of 735°F (391°C) for the Teflon seal and 800°F (427°C) for the metallic helicoflex seal. Similarly, the peak temperature of 1407°F (764°C) predicted for the vent and drain port seals at approximately 6.8 hours elapsed time, exceeds the rated long-term service temperature of the Teflon seal material.

Because the predicted temperatures exceed the long-term service temperature of the seals, the seals are assumed to fail, and there is a potential for release of radioactive CRUD particles from the package. The amount of releasable CRUD in the NAC LWT package was determined based on contents consisting of one PWR fuel assembly containing 289 fuel rods. An estimate of the maximum "spot" CRUD activity shows that for 90% of PWR spent fuel rods the maximum activity is $20\mu Ci/cm^2$ or less [26, Table I-15]. The ratio of the peak (i.e., the maximum "spot" CRUD activity) to average concentration on the rod surface varies by a factor of two for PWR fuel rods [26, Table I-12].

The CRUD activity estimates [26] are based on newly discharged spent nuclear fuel. The CRUD activity is expected to decay by a factor of one-half for five-year cooled fuel, based on the decay rate for Co^{60}. This proves to be a good approximation because 92% of the activity for five-year cooled PWR fuel comes from Co^{60}. The majority of the remaining 8% of the activity comes from Mn^{54}. By the time the fuel has aged 8 years, Co^{60} represents 99% of the activity.

Based on these data, the average CRUD activity for a PWR rod with a surface area of 1200 cm^2 is about 0.006 curies for five-year cooled fuel. The average CRUD activity for a 17 x 17 PWR assembly is about 1.73 Ci. The amount of CRUD that would flake or spall from the surface of a PWR rod due to temperatures calculated for the fuel rods in the thermal analysis is estimated to be a maximum of 15% [26, Table I-10]. Finally, a deposition factor of 0.90 was used to account for the deposition of CRUD particles on package surfaces and fuel assemblies.

The major assumptions used to estimate CRUD release are given in Table 8.4. The potential release from the NAC LWT package can be estimated from the same equation used for the TN-68 release estimate, as described in Section 8.2.1. The major driving force for material release results from the increased fill gas pressure inside the package due to increases in internal temperature. The temperature change is bounded by the difference between the maximum fill gas temperature predicted during the fire transient and the fill gas temperature inside the package at the time the package is loaded.

For this analysis, the loading temperature is defined as 100°F (38°C), based on the temperature reported in the SAR [11]. The maximum fill gas temperature is conservatively assumed to be the maximum inner shell temperature, predicted during the transient, of 1261 °F (683 °C). Table 8.5 shows the results obtained when this equation is applied using the parameter values from Table 8.4 and the temperatures predicted for the NAC LWT package in this accident scenario.

Table 8.4. Assumptions Used for Release Estimate for NAC LWT Package

Parameter	Assumed value
Number of Assemblies in Package	1 PWR
Rods per Assembly	289
Maximum "spot" CRUD Activity on Fuel Rod	20μCi/cm^2
Peak to axial average variation	2
CRUD decay factor (5 yr; based on Co^{60})	0.5
Average surface area per rod	1200 cm^2
Average CRUD Activity on PWR Fuel Rod (5 yr cooled)	0.006 Ci
Average CRUD Activity on PWR Assembly (5 yr cooled)	1.73 Ci
Fraction of CRUD released due to heating	0.15
Deposition Factor	0.90

Table 8.5. Potential Release Estimate for NAC LWT Package

Initial temperature °F (°R)	Peak temperature °F (°R)	Potential release (curies)
100 (560)	1261 (1721)	0.02

The potential CRUD release from the NAC LWT package based on five-year cooled fuel is estimated to be approximately 0.02 curies of Co^{60}. Since the A_2 value for Co^{60} is 11 curies, the potential release is about 0.002 of an A_2 quantity (see Section 8.2).

8.2.5 Potential Releases from Packages Carrying Failed Fuel

Of the three packages considered in this evaluation, only the HI-STAR 100 and the NAC LWT are approved to carry any kind of failed fuel. As discussed in Section 8.2.2, the HI-STAR 100 employs a welded inner multipurpose canister (MPC) that maintains its integrity throughout the entire fire transient. Even with failed fuel in this package, no fission products or fuel fines would be released into the cavity of the overpack for the conditions encountered in the Baltimore tunnel fire scenario. Therefore, no radioactive material from this package would be available for release into the environment. The NAC LWT, however, does present a possible path for the release of fission gasses and/or fuel fines, should a failed fuel payload be subject to conditions as severe as the tunnel fire analyzed in this study.

The staff did not analyze how a shipment of failed fuel would affect the release of spent fuel constituents from the NAC LWT. However, analyses presented in NUREG/CR-6672 [25] investigated the effect of an extraordinarily severe fully engulfing fire lasting 11 hours at 1832°F (1000°C) on a generic truck package that was based on the design of the NAC LWT. In this fire analysis for the generic truck package, it was assumed that 100% of the rods of a single PWR assembly failed due to thermal rupture. The resulting fission product release from the rods very conservatively bounds the potential release from any shipment of failed fuel rods that the NAC LWT would be allowed to carry.

The analysis in NUREG/CR-6672 [25] predicted the potential release fractions of various spent fuel constituents for a generic truck package carrying a single PWR spent fuel assembly consisting of high burn-up 3-year-cooled fuel. This assembly is far hotter than any fuel the NAC LWT is licensed to carry, and has a total activity of 7.9×10^4 Curies of Cesium 137 (Cs^{137}) (see Table 7.9 of NUREG/CR-6672 [25]). Using the release fraction 1.7×10^{-5} (see Table 7.31 of NUREG/CR-6672 [25]) for Cs^{137} calculated for the truck package, the estimated total release is approximately 1.3 Curies. This value is considerably larger than the estimated release for the fuel licensed for transport in the NAC LWT package (see Table 8.5), but is still far below the A_2 quantity of 16 Curies for Cs^{137} (see Section 8.2).

The analysis for Cs^{137}, as well as similar analyses conducted for particulates and the radionuclide Ruthenium (Ru) in NUREG/CR-6672[25], indicate that the potential release from the NAC LWT for any fuel that it is licensed to carry, whether intact or failed, would be small, even for conditions as severe as those encountered in the Baltimore tunnel fire.

8.3 Summary of Potential Releases

The results of the NTSB investigation of the Howard Street tunnel fire, the FDS tunnel fire model developed by NIST, and the rail car material fire exposure analyses completed by CNWRA, have provided a detailed picture of the potential duration and severity of the fire that occurred in the Howard Street tunnel in Baltimore on July 18, 2001. The fire transient analyses performed with ANSYS and COBRA-SFS using the FDS simulation results as boundary conditions have shown the robust nature of the larger spent fuel transportation package designs (HI-STAR 100 and TN-68). The predicted response of the smaller LWT package, if hauled by rail and exposed to the same tunnel fire environment, indicates more component degradation, but even this package survives the fire scenario without exceeding temperature limits for fuel cladding integrity or regulatory limits for radiological consequences.

For the TN-68 and the NAC LWT, the maximum temperatures predicted in the regions of the lid and the vent and drain ports exceed the seals' rated service temperatures, making it possible for a small release to occur, due to CRUD that might detach from the surfaces of the fuel rods. A release is not expected in this accident scenario, due to a number of factors, including (1) the tight clearances maintained between the lid and package body by the closure bolts, (2) the low pressure differential between the package interior and exterior, (3) the tendency of such small clearances to plug, and (4) the tendency of CRUD particles to settle or plate out. However, the above analysis shows that if a release were to occur, it would be within regulatory limits.

USNRC staff evaluated the radiological consequences of the package responses to the Baltimore tunnel fire. The results of this evaluation strongly indicate that neither spent nuclear fuel (SNF) particles nor fission products would be released from a spent fuel transportation package carrying intact spent fuel involved in a severe tunnel fire such as the Howard Street tunnel fire in Baltimore. None of the three package designs analyzed for the Baltimore Tunnel fire scenario (TN-68, HI-STAR 100, and NAC LWT) experienced internal temperatures that would result in rupture of the fuel cladding. In all three packages, the peak fuel cladding temperature is conservatively predicted to remain below the short-term limit of 1058°F (570°C).

Therefore, radioactive material (i.e., SNF particles or fission products) would be retained within the fuel rods. There would be no release from the HI-STAR 100, because the inner welded canister remains leak tight and all seals remain intact. The potential releases calculated for the TN-68 package and the NAC LWT package (as a consequence of exceeding seal temperature limits) indicate that any release of CRUD from either package would be very small - less than an A_2 quantity (see Section 8.2).

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Appendix A

Material Properties for COBRA-SFS Model of TN-68 Package

Table A.1. Internal Fill Gas—Helium at Atmospheric Pressure

Temperature (°F)	Enthalpy (Btu/lbm)	Thermal Conductivity (Btu/hr-ft-°F)	Specific Heat (Btu/lbm-°F)	Specific Volume (ft^3/lbm)	Viscosity (lbm/hr-ft)
0	100	0.078	1.24	83.33	0.0410
200	348	0.097	1.24	119.76	0.0533
400	596	0.115	1.24	156.25	0.0641
600	844	0.129	1.24	192.31	0.0727
800	1092	0.138	1.24	229.36	0.0823
1000	1340	0.138	1.24	265.25	0.0907
2552	3264	0.138	1.24	549.00	0.1138

Table A.2. External Ambient Air at Atmospheric Pressure

Temperature (°F)	Enthalpy (Btu/lbm)	Thermal Conductivity (Btu/hr-ft-°F)	Specific Heat (Btu/lbm-°F)	Specific Volume (ft^3/lbm)	Viscosity (lbm/hr-ft)
60	124.5	0.0146	0.24	13.5669	0.0434
300	182.1	0.0193	0.243	19.8325	0.058
400	206.5	0.0212	0.245	22.4432	0.063
500	231.1	0.0231	0.247	25.0539	0.068
600	256	0.025	0.25	27.6645	0.072
700	281.1	0.0268	0.253	30.2752	0.077
800	306.7	0.0286	0.256	32.8859	0.081
900	332.5	0.0303	0.259	35.4966	0.085
1000	358.6	0.0319	0.262	38.1072	0.0889
2000	617.2	0.0471	0.2586	64.214	0.1242
4000	1522	0.0671	0.4524	116.428	0.1242

Table A.3. Summary of All Solid Material Properties Pre-Fire

Specific Heat (Btu/lbm-°F)	Density (lbm/ft^3)	Thermal Conductivity (Btu/hr-ft-°F)	Emissivity	Description
0.129	483.8	22.92	0.3	gamma shielding (SA-517 grade 70 carbon steel)
0.13	499.4	10.44	0.3	fuel tubes (SA-240 stainless steel)
0.214	165.9	41.72	0.3	borated aluminum poison plates
0.311	98.5	4.34	N/A	neutron shield (borated polyester)
0.228	165.9	99.84	0.3	Aluminum alloy basket rails
0.118	483.8	22.92	0.3	cask outer shell[a]
0.228	165.9	84.00	N/A	aluminum in neutron shield and thermal shield between cask and bottom impact limiter
0.420	23.1	0.064	N/A	wooden impact limiters (covered with sheet steel)
0.420	11.0	0.053	N/A	thin top layer of wood on impact limiter ends (covered with sheet steel)

[a] Based on nominal emissivity for carbon steel. SAR analyses use emissivity of 0.9 for painted cask surface, but cask specifications allow option for unpainted outer surface.

Table A.4. Summary of All Solid Material Properties Post-Fire

Specific Heat (Btu/lbm-°F)	Density (lbm/ft^3)	Thermal Conductivity (Btu/hr-ft-°F)	Emissivity	Description
0.129	483.8	22.92	0.3	gamma shielding (SA-517 grade 70 carbon steel)
0.13	499.4	10.44	0.3	fuel tubes (SA-240 stainless steel)
0.214	165.9	41.72	0.3	borated aluminum poison plates
0.26	0.027	0.03	N/A	hot air (replaces polyresin neutron shield vaporized in fire)
0.228	165.9	99.84	0.3	aluminum alloy basket rails
0.118	483.8	22.92	0.8	steel shell (SAR value post-fire is 0.95 for charred cask surface emissivity)
0.228	165.9	84.00	0.9	aluminum in neutron shield; inner and outer ring after polyresin evaporates
1020.0	134.8	0.00735	0.8	charcoal (impact limiters after the fire)
			0.9	tunnel wall

COBRA-SFS Material Properties Compared with Published SAR Values

Table A.5. BWR Spent Fuel Assemblies

SAR values determined using k-effective model for homogeneous representation of fuel rods and helium gas within fuel tube.

Temperature (°F)	Transverse Thermal Conductivity (Btu/hr-ft-°F)	Axial Thermal Conductivity (Btu/hr-ft-°F)	Specific Heat (Btu/lbm-°F)	Density (lbm/ft^3)
195.8	0.0157		0.055	257.5
200.0		0.058		
268.4	0.0178			
365.9	0.0206			
400.0		0.0646		
463.7	0.0239			
561.8	0.0277			
600.0		0.0709		
660.3	0.0319			
758.9	0.0367			
800.0		0.0769	0.055	257.5

COBRA-SFS input— BWR fuel rods; conservative values at nominal operating temperature and above.

Component	Thermal Conductivity (Btu/hr-ft-°F)	Specific Heat (Btu/lbm-°F)	Density (lbm/ft^3)
fuel pellet:	3.0	0.059	655.0
cladding:	10.0	0.1	409.0

Table A.6. Stainless Steel Type 304/304L (for fuel tubes)

SAR values			
Temperature (°F)	**Thermal Conductivity (Btu/hr-ft-°F)**	**Specific Heat (Btu/lbm-°F)**	**Density (lbm/ft^3)**
70	7.56	0.111	499.4
100	8.76		
200	9.36	0.124	
400	10.44	0.130	
600	11.28	0.134	
800	12.24	0.140	
1000	13.2		499.4
COBRA-SFS input—selected conservative representative values at nominal operating temperature and above			
all	10.44	0.13	499.4

Table A.7. Poison Plates (borated aluminum or boron carbide/aluminum matrix)

SAR values			
Temperature (°F)	**Thermal Conductivity (Btu/hr-ft-°F)**	**Specific Heat (Btu/lbm-°F)**	**Density (lbm/ft^3)**
68	69.36	0.214	169.3
212	83.76		
482	86.64		
571	86.64	0.214	169.3
COBRA-SFS input—selected conservative values based on range of allowable fabrication variations, as described for cask specifications in SAR.			
all	41.72	0.214	165.9

Table A.8. Aluminum Type 6060 (for basket support rails and shims)

SAR values			
Temperature (°F)	**Thermal Conductivity (Btu/hr-ft-°F)**	**Specific Heat (Btu/lbm-°F)**	**Density (lbm/ft^3)**
70	96.12	0.218	165.9
100	96.96	0.219	
150	98.04	0.223	
200	99	0.225	
250	99.84	0.228	
300	100.56	0.23	
350	101.28	0.233	
400	101.88	0.234	165.9
COBRA-SFS input—selected conservative representative values at nominal operating temperature and above.			
all	99.84	0.228	165.9

Table A.9. Carbon Steel SA-516 Grade 70 (for inner and outer gamma shield and lid)

<table>
<tr><td colspan="4">SAR values</td></tr>
<tr><th>Temperature (°F)</th><th>Thermal Conductivity (Btu/hr-ft-°F)</th><th>Specific Heat (Btu/lbm-°F)</th><th>Density (lbm/ft^3)</th></tr>
<tr><td>70</td><td>22.92</td><td>0.109</td><td>483.8</td></tr>
<tr><td>200</td><td>23.76</td><td>0.118</td><td></td></tr>
<tr><td>400</td><td>23.88</td><td>0.129</td><td></td></tr>
<tr><td>600</td><td>22.92</td><td>0.139</td><td></td></tr>
<tr><td>800</td><td>21.6</td><td>0.152</td><td></td></tr>
<tr><td>1000</td><td>20.16</td><td>0.169</td><td></td></tr>
<tr><td>1200</td><td>18.24</td><td>0.206</td><td></td></tr>
<tr><td>1400</td><td>15.48</td><td>0.184</td><td>483.8</td></tr>
<tr><td colspan="4">COBRA-SFS input—selected conservative representative values at nominal operating temperature and above.</td></tr>
<tr><td>all</td><td>22.92</td><td>0.129</td><td>483.8</td></tr>
</table>

Table A.10. Neutron Shield (polyester resin with aluminum boxes)

<table>
<tr><td colspan="4">SAR values—properties are composite values for polyester resin and aluminum boxes modeled as single homogeneous material.</td></tr>
<tr><th>Temperature (°F)</th><th>Thermal Conductivity (Btu/hr-ft-°F)</th><th>Specific Heat (Btu/lbm-°F)</th><th>Density (lbm/ft^3)</th></tr>
<tr><td>all</td><td>0.0996</td><td>0.311</td><td>98.5</td></tr>
<tr><td colspan="4">COBRA-SFS input—selected conservative representative values at nominal operating temperature and above.</td></tr>
<tr><td>borated polyester</td><td>4.34</td><td>0.311</td><td>98.5</td></tr>
<tr><td>aluminum</td><td>84.00</td><td>0.228</td><td>165.9</td></tr>
</table>

Table A.11. Carbon Steel SA-350 grade LF3 (for cask outer shell)

<table>
<tr><td colspan="4">SAR values</td></tr>
<tr><th>Temperature (°F)</th><th>Thermal Conductivity (Btu/hr-ft-°F)</th><th>Specific Heat (Btu/lbm-°F)</th><th>Density (lbm/ft^3)</th></tr>
<tr><td>70</td><td>23.64</td><td>0.106</td><td>489.0</td></tr>
<tr><td>100</td><td>23.88</td><td>0.11</td><td></td></tr>
<tr><td>200</td><td>24.36</td><td>0.118</td><td></td></tr>
<tr><td>400</td><td>24.24</td><td>0.128</td><td></td></tr>
<tr><td>600</td><td>23.16</td><td>0.137</td><td></td></tr>
<tr><td>800</td><td>21.72</td><td>0.149</td><td></td></tr>
<tr><td>1000</td><td>20.04</td><td>0.165</td><td></td></tr>
<tr><td>1200</td><td>18.24</td><td>0.189</td><td></td></tr>
<tr><td>1400</td><td>15.36</td><td>0.406</td><td>489.0</td></tr>
<tr><td colspan="4">COBRA-SFS input- typical values for carbon steel at nominal operating temperature and above, based on range of allowable fabrication variations described for cask specifications in SAR.</td></tr>
<tr><td>all</td><td>22.92</td><td>0.118</td><td>483.8</td></tr>
</table>

Table A.12. Impact Limiters (wood covered with sheet steel)

<table>
<tr><td colspan="4">SAR values—none provided; SAR analyses assume impact limiters act as perfect insulators on cask ends for normal, off-normal, and fire accident conditions.</td></tr>
<tr><td colspan="4">COBRA-SFS input—selected conservative representative values at nominal operating temperature and above.</td></tr>
<tr><th>Material</th><th>Thermal Conductivity (Btu/hr-ft-°F)</th><th>Specific Heat (Btu/lbm-°F)</th><th>Density (lbm/ft³)</th></tr>
<tr><td>redwood</td><td>0.064</td><td>0.311</td><td>98.5</td></tr>
<tr><td>balsa</td><td>0.053</td><td>0.228</td><td>165.9</td></tr>
<tr><td>carbon steel</td><td>22.92</td><td>0.118</td><td>483.8</td></tr>
<tr><td>charcoal</td><td>0.00735</td><td>1020.0</td><td>134.8</td></tr>
</table>

Table A.13. Air (replacing neutron shield polyethylene after fire)

<table>
<tr><td colspan="4">SAR values</td></tr>
<tr><th>Temperature (°F)</th><th>Thermal Conductivity (Btu/hr-ft-°F)</th><th>Specific Heat (Btu/lbm-°F)</th><th>Density (lbm/ft³)</th></tr>
<tr><td>81</td><td>0.0156</td><td>0.231</td><td>0.0734</td></tr>
<tr><td>261</td><td>0.0192</td><td>0.237</td><td>0.0551</td></tr>
<tr><td>441</td><td>0.0228</td><td>0.239</td><td>0.0440</td></tr>
<tr><td>621</td><td>0.0264</td><td>0.246</td><td>0.0367</td></tr>
<tr><td>981</td><td>0.0336</td><td>0.264</td><td>0.0275</td></tr>
<tr><td colspan="4">COBRA-SFS input—selected representative values at immediate post-fire temperature and above.</td></tr>
<tr><td>all</td><td>0.03</td><td>0.26</td><td>0.0270</td></tr>
</table>

Appendix B

Material Properties for ANSYS Model of HI-STAR 100 Package

Table B.1. Homogeneous Fuel Region for Westinghouse 17x17 OFA

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
0	0.04412	0.04412	0.06256	0.14353	0.05869	Fuel Region (2.25 multiplier against helium contribution to account for limited convection and pressurization enhancement)
100	0.04412	0.04412	0.06256	0.14353	0.05869	
200	0.04412	0.04412	0.06256	0.14352	0.05869	
300	0.05078	0.05078	0.06509	0.14352	0.05869	
400	0.05895	0.05895	0.06797	0.14352	0.05869	
500	0.06837	0.06837	0.07082	0.14352	0.05869	
600	0.07834	0.07834	0.07391	0.14352	0.05869	
700	0.08920	0.08920	0.07756	0.14352	0.05869	
800	0.09508	0.09508	0.08121	0.15352	0.05869	
900	0.09508	0.09508	0.08484	0.15352	0.05869	
1000	0.09508	0.09508	0.08600	0.15352	0.05869	

Table B.2. Alloy-X

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
200	0.70000	*	*	0.28993	0.12000	Basket Plates, Basket Supports, Boral Plate Sheathing, MPC shell, impact limiter skin shell
450	0.81667	*	*			
700	0.91667	*	*			
1400	1.19670	*	*			

Table B.3. Helium

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
0	0.00650	*	*	6.90E-06	1.24000	gas conduction between MPC and cask
200	0.00808	*	*	4.81E-06		
400	0.00958	*	*	3.69E-06		
600	0.01075	*	*	2.99E-06		
800	0.01150	*	*	2.52E-06		
1400	0.01370	*	*	1.71E-06		

Table B.4. Helium
(with 2.25 multiplier to account for limited convection and pressurization enhancement)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
0	0.01400	*	*	6.90E-06	1.24000	Conduction in: central core region, between guide tubes and basket plates, between fuel and compartments, and between basket and MPC Shell
200	0.01740	*	*	4.81E-06		
400	0.02063	*	*	3.69E-06		
600	0.02315	*	*	2.99E-06		
800	0.02476	*	*	2.52E-06		
1400	0.02950	*	*	1.71E-06		

Table B.5. Boral Plates
(includes 0.004" helium gap and gap radiation on both sides of Boral)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
0	0.30836	4.62020	4.62020	0.08390	0.24762	parallel to thickness (switch x & y to define cross-width)
100	0.34331	4.62550	4.62550	0.08390		
200	0.37738	4.64850	4.64850	0.08390		
300	0.40969	4.69040	4.69040	0.08390		
400	0.44166	4.73250	4.73250	0.08390		
500	0.46611	4.74620	4.74620	0.08390		
600	0.49024	4.75200	4.75200	0.08390		
700	0.50544	4.73700	4.73700	0.08390		
800	0.52053	4.72210	4.72210	0.08390		
900	0.53517	4.70710	4.70710	0.08390		
1000	0.54970	4.69220	4.69220	0.08390		
1100	0.56438	4.68350	4.68350	0.08390		

Table B.6. Carbon Steel (SA-516, Gr. 70)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (radial)	Thermal Conductivity (Btu/hr-in-°F) (circumferential)	Thermal Conductivity (Btu/hr-in-°F) (axial)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
200	0.17409	2.03330	2.03330	0.28299	0.10000	Gamma Shield with 0.01" air gaps between plates
450	0.22634	1.99170	1.99170			
700	0.28273	1.86670	1.86670			
1400	0.44136	1.46670	1.46670			

Table B.7. Carbon Steel (SA-515, Gr. 70)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
200	2.43330	*	*	0.28299	0.10000	For radial channels of overpack and enclosure of shells of overpack (Fins)
450	2.25830	*	*			
700	2.05000	*	*			
1400	1.46670	*	*			

Table B.8. Holtite-A

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
*	0.03108	*	*	0.06076	0.39000	Neutron Shield/In impact limiter

Table B.9. HT-870

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
*	0.00340	*	*	0.00868	0.39000	Foam on back side of fins

Table B.10. Air Properties Representing Degraded Materials

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
200	0.00148	*	*	3.48E-05	0.24110	For degraded Holtite-A, HT-870, and Honeycomb after fire
450	0.00188	*	*	2.53E-05	0.24605	
700	0.00227	*	*	1.99E-05	0.25355	
1400	0.00336	*	*	1.31E-05	0.27445	

Table B.11. One-Quarter-Inch Fillet Weld - Carbon Steel (SA-515, Gr. 70)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
200	1.21670	2.43330	2.43330	0.28299	0.10000	Reduced radial channel conductivity (Fin Fillet Weld Root)
450	1.12920	2.25830	2.25830			
700	1.02500	2.05000	2.05000			
1400	0.73333	1.46670	1.46670			

Table B.12. Carbon Steel (SA-516, Gr. 70)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
200	2.03330	*	*	0.28299	0.10000	Gamma Shield (intimate contact) and impact limiter base structure
450	1.99170	*	*			
700	1.86670	*	*			
1400	1.46670	*	*			

Table B.13. Aluminum Honeycomb
(700 psi unidirectional w/1700 psi cross-core backing)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
68	1.11710	0.47427	1.11710	0.01406	0.20800 (assumed)	Type 1: Aluminum Honeycomb
212	1.15270	0.48944	1.15270	0.01406		
752	1.42620	0.59537	1.42620	0.01406		
1400	1.75440	0.72248	1.75440	0.01406		

Table B.14. Aluminum Honeycomb
(700 psi unidirectional and 2300 psi cross-core)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
68	0.82721	0.31682	0.82721	0.00579	0.20800 (assumed)	Type 2&5: Aluminum Honeycomb
212	0.85369	0.32693	0.85369	0.00579		
752	1.03810	0.39771	1.03810	0.00579		
1400	1.25940	0.48265	1.25940	0.00579		

Table B.14. Aluminum Honeycomb
(2300 psi cross-core)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
68	1.40690	0.63172	1.40690	0.01684	0.20800 (assumed)	Type 3: Aluminum Honeycomb
212	1.45170	0.65194	1.45170	0.01684		
752	1.81430	0.79302	1.81430	0.01684		
1400	2.24930	0.96231	2.24930	0.01684		

Table B.16. Aluminum Honeycomb
(1100 psi unidirectional and 2300 psi cross-core)

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F) (x)	Thermal Conductivity (Btu/hr-in-°F) (y)	Thermal Conductivity (Btu/hr-in-°F) (z)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
68	1.40690	0.63172	1.40690	1.40630	0.20800 (assumed)	Type 4: Aluminum Honeycomb
212	1.45170	0.65194	1.45170	1.40630		
752	1.81430	0.79302	1.81430	1.40630		
1400	2.24930	0.96231	2.24930	1.40630		

Table B.17. Emissivity Values for Radiation Heat Transfer

Component	Material	Emissivity
Fuel	Zircaloy	0.80
Basket	Alloy-X	0.36
Support Bracket	Alloy-X	0.36
MPC Wall	Alloy-X	0.36
Borated Aluminum Plate	Boral	0.55
Bare Carbon Steel	Carbon Steel	0.65
Painted Surfaces		0.90
Cask and Impact Limiter Surfaces	Alloy-X	0.36
Tunnel Surface		0.90
Soot Surfaces		0.90

Appendix C

Material Properties for ANSYS Model of Legal Weight Truck Package

Table C.1. 304 Stainless Steel

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
70	0.7143	-	0.1141	Used for cask body, cask lid, spokes
212	0.7800	0.2888	0.1207	
392	0.8592	0.2872	0.1272	
572	0.9333	0.2855	0.1320	
752	1.0042	0.2839	0.1356	
932	1.0717	0.2822	0.1385	
1112	1.1375	0.2805	0.1412	

Table C.2. 6061-T6 Aluminum

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
32	9.7500	0.0984	0.2140	Used for basket, IL 1, 2 skin
212	9.9167			
572	11.0833			
932	12.9167			

Table C.3. 6061-T6 Aluminum Honeycomb

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
32	1.6965	0.017118056	0.214	Used for IL 1 (Honeycomb)
212	1.7255			
572	1.9285			
932	2.2475			

Table C.4. 6061-T6 Aluminum Honeycomb

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
32	1.4235	0.0144	0.214	Used for IL 2 (Honeycomb)
212	1.4478			
572	1.6182			
932	1.8858			

Table C.5. Helium

Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F)	Density (lbm/in^3)	Specific Heat (Btu/lbm-°F)	Description
200	0.00808	4.83E-06	1.24	Used for cask gap and fuel gap
400	0.00942	3.70E-06		
600	0.01075	3.01E-06		
800	0.0115	2.52E-06		

Table C.6. Lead Gamma Shield

Temperature (°F)	Enthalpy(1) (Btu/lbm)	Temperature (°F)	Thermal Conductivity(2) (Btu/hr-in-°F)	Temperature (°F)	Density(3) (lbm/in^3)	Description
80.33	0.0860	80.3	1.698984	53.3	4.11060E-01	Used for lead gamma shield
260.33	5.7610	170.3	1.671552	233.3	4.07470E-01	
440.33	11.608	260.3	1.641888	413.3	4.03670E-01	
611.50	17.756	350.3	1.608588	607.7	3.99450E-01	
629.50	27.730	440.3	1.573092	622.1	3.84440E-01	
800.33	34.007	530.3	1.539792	802.1	3.80740E-01	
980.33	40.241	610.3	1.515924	982.1	3.76330E-01	
1160.33	46.432	630.3	0.746712	1162.1	3.71930E-01	
1340.33	52.580	710.3	0.796428	1342.1	3.67520E-01	
1520.33	58.641	800.3	0.84222	1522.1	3.63120E-01	
		890.3	0.884016			
		980.3	0.921852			
		1070.3	0.955764			
		1160.3	0.985716			
		1250.3	1.01171			
		1340.3	1.03378			

(1) Based on specific heat from B.J. McBride, S. Gordon and M.A. Reno, NASA Technical Paper 3287, (1993). Enthalpy as a function of temperature calculated using definition of specific heat as partial derivative of enthalpy with respect to temperature at constant pressure;

$$c_p = \left(\frac{\partial h}{\partial T}\right)_p$$

(2) C.Y. Ho, R.W. Powell and P.E. Liley, J. **Phys. Chem. Ref. Data**, v1, p279 (1972).

(3) F.C. Nix and D. MacNair, Physical Review, v60, p597 (1941) and R. Feder, A.S. Norwick, Physical Review, v109, p1959 (1958); calculated from the linear expansion.

Table C.7. 56% Ethylene Glycol Solution

Avg. Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F)	Specific Heat (Btu/lbm-°F)	Density (lbm/in^3)
50	0.0188	0.7405	0.0391
70	0.0187	0.7522	0.0389
100	0.0185	0.7696	0.0385
150	0.0182	0.7979	0.0378
200	0.0179	0.8255	0.0370
250	0.0177	0.8522	0.0362
260	0.0176	0.8575	0.0360
270	0.0176	0.8627	0.0358
280	0.0175	0.8679	0.0357
290	0.0175	0.8731	0.0355
300	0.0174	0.8782	0.0353
310	0.0174	0.8833	0.0351
320	0.0173	0.8884	0.0349
330	0.0173	0.8934	0.0347
340	0.0172	0.8984	0.0345
350	0.0172	0.9034	0.0343

Table C.8. Air

Avg. Temperature (°F)	Thermal Conductivity (Btu/hr-in-°F)	Specific Heat (Btu/lbm-°F)	Density (lbm/in^3)
350	0.0017	0.2467	0.0000283
450	0.0018	0.2494	0.0000252
550	0.0020	0.2516	0.0000227
650	0.0022	0.2533	0.0000206
750	0.0023	0.2546	0.0000189
850	0.0025	0.2556	0.0000175
950	0.0026	0.2562	0.0000162
1050	0.0027	0.2566	0.0000152
1150	0.0029	0.2568	0.0000142
1250	0.0030	0.2570	0.0000134
1350	0.0031	0.2571	0.0000126
1450	0.0033	0.2571	0.0000120
1550	0.0034	0.2573	0.0000114
1650	0.0035	0.2576	0.0000108
1750	0.0036	0.2581	0.0000104
1850	0.0038	0.2589	0.0000099
1950	0.0039	0.2599	0.0000095
2050	0.0040	0.2614	0.0000091

Table C.9. Effective Conductivity for Liquid Neutron Shield with 1°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	0.364	0.149	0.003	0.002
260	0.374	0.153	0.003	0.002
270	0.384	0.157	0.003	0.002
280	0.393	0.161	0.003	0.002
290	0.398	0.163	0.003	0.002
300	0.396	0.162	0.003	0.002
310	0.395	0.162	0.003	0.002
320	0.394	0.161	0.003	0.002
330	0.393	0.161	0.003	0.002
340	0.391	0.160	0.003	0.002
350	0.390	0.160	0.003	0.002
351	*	*	0.003	0.002
400	*	*	0.003	0.002
500	*	*	0.003	0.002
600	*	*	0.003	0.002
700	*	*	0.003	0.002
800	*	*	0.003	0.002
1000	*	*	0.003	0.003
1200	*	*	0.003	0.003
1500	*	*	0.003	0.003
2000	*	*	0.004	0.004
2500	*	*	0.004	0.004

Table C.10. Effective Conductivity for Liquid Neutron Shield with 10°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	0.654	0.268	0.006	0.002
260	0.673	0.276	0.006	0.002
270	0.691	0.283	0.006	0.002
280	0.704	0.288	0.006	0.002
290	0.705	0.289	0.006	0.002
300	0.703	0.288	0.006	0.002
310	0.701	0.287	0.006	0.002
320	0.699	0.286	0.006	0.002
330	0.697	0.286	0.006	0.002
340	0.695	0.285	0.006	0.002
350	*	*	0.006	0.002
351	*	*	0.006	0.002
400	*	*	0.006	0.002
500	*	*	0.006	0.002
600	*	*	0.005	0.002
700	*	*	0.005	0.002
800	*	*	0.005	0.002
1000	*	*	0.005	0.003
1200	*	*	0.005	0.003
1500	*	*	0.004	0.003
2000	*	*	0.004	0.004
2500	*	*	0.004	0.004

Table C.11. Effective Conductivity for Liquid Neutron Shield with 25°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	0.840	0.344	0.008	0.003
260	0.863	0.353	0.008	0.003
270	0.882	0.361	0.008	0.003
280	0.888	0.364	0.008	0.003
290	0.885	0.363	0.007	0.003
300	0.883	0.361	0.007	0.003
310	0.880	0.360	0.007	0.003
320	0.877	0.359	0.007	0.003
330	0.875	0.358	0.007	0.003
340	0.872	0.357	0.007	0.003
350	*	*	0.007	0.003
351	*	*	0.007	0.003
400	*	*	0.007	0.003
500	*	*	0.007	0.003
600	*	*	0.007	0.003
700	*	*	0.007	0.003
800	*	*	0.006	0.003
1000	*	*	0.006	0.003
1200	*	*	0.006	0.003
1500	*	*	0.005	0.003
2000	*	*	0.005	0.004
2500	*	*	0.005	0.004

Table C.12. Effective Conductivity for Liquid Neutron Shield with 50°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	1.061	0.434	0.009	0.004
260	1.058	0.433	0.009	0.004
270	1.055	0.432	0.009	0.004
280	1.052	0.431	0.009	0.004
290	1.049	0.430	0.009	0.004
300	1.046	0.428	0.009	0.004
310	1.043	0.427	0.009	0.004
320	1.039	0.426	0.009	0.004
330	*	*	0.009	0.004
340	*	*	0.009	0.004
350	*	*	0.009	0.004
351	*	*	0.009	0.004
400	*	*	0.009	0.003
500	*	*	0.008	0.003
600	*	*	0.008	0.003
700	*	*	0.008	0.003
800	*	*	0.008	0.003
1000	*	*	0.007	0.003
1200	*	*	0.007	0.003
1500	*	*	0.006	0.003
2000	*	*	0.006	0.004
2500	*	*	0.006	0.004

Table C.13. Effective Conductivity for Liquid Neutron Shield with 70°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	1.151	0.471	0.010	0.004
260	1.148	0.470	0.010	0.004
270	1.144	0.469	0.010	0.004
280	1.141	0.467	0.010	0.004
290	1.138	0.466	0.010	0.004
300	1.134	0.464	0.010	0.004
310	1.131	0.463	0.010	0.004
320	*	*	0.010	0.004
330	*	*	0.010	0.004
340	*	*	0.009	0.004
350	*	*	0.009	0.004
351	*	*	0.009	0.004
400	*	*	0.009	0.004
500	*	*	0.009	0.004
600	*	*	0.009	0.004
700	*	*	0.008	0.003
800	*	*	0.008	0.003
1000	*	*	0.008	0.003
1200	*	*	0.007	0.003
1500	*	*	0.007	0.003
2000	*	*	0.006	0.004
2500	*	*	0.006	0.004

Table C.14. Effective Conductivity for Liquid Neutron Shield with 100°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	1.253	0.513	0.011	0.004
260	1.249	0.512	0.011	0.004
270	1.245	0.510	0.011	0.004
280	1.242	0.509	0.011	0.004
290	1.238	0.507	0.011	0.004
300	1.234	0.505	0.011	0.004
310	*	*	0.010	0.004
320	*	*	0.010	0.004
330	*	*	0.010	0.004
340	*	*	0.010	0.004
350	*	*	0.010	0.004
351	*	*	0.010	0.004
400	*	*	0.010	0.004
500	*	*	0.010	0.004
600	*	*	0.009	0.004
700	*	*	0.009	0.004
800	*	*	0.009	0.004
1000	*	*	0.008	0.003
1200	*	*	0.008	0.003
1500	*	*	0.008	0.003
2000	*	*	0.007	0.004
2500	*	*	0.007	0.004

Table C.15. Effective Conductivity for Liquid Neutron Shield with 200°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	1.468	0.601	0.013	0.005
260	*	*	0.013	0.005
270	*	*	0.013	0.005
280	*	*	0.013	0.005
290	*	*	0.013	0.005
300	*	*	0.012	0.005
310	*	*	0.012	0.005
320	*	*	0.012	0.005
330	*	*	0.012	0.005
340	*	*	0.012	0.005
350	*	*	0.012	0.005
351	*	*	0.012	0.005
400	*	*	0.012	0.005
500	*	*	0.012	0.005
600	*	*	0.011	0.004
700	*	*	0.011	0.004
800	*	*	0.011	0.004
1000	*	*	0.010	0.004
1200	*	*	0.010	0.004
1500	*	*	0.009	0.004
2000	*	*	0.008	0.004
2500	*	*	0.008	0.005

Table C.16. Effective Conductivity for Liquid Neutron Shield with 300°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	*	*	0.014	0.005
260	*	*	0.014	0.005
270	*	*	0.014	0.005
280	*	*	0.014	0.005
290	*	*	0.014	0.005
300	*	*	0.014	0.005
310	*	*	0.014	0.005
320	*	*	0.014	0.005
330	*	*	0.014	0.005
340	*	*	0.014	0.005
350	*	*	0.013	0.005
351	*	*	0.013	0.005
400	*	*	0.013	0.005
500	*	*	0.013	0.005
600	*	*	0.012	0.005
700	*	*	0.012	0.005
800	*	*	0.012	0.005
1000	*	*	0.011	0.004
1200	*	*	0.011	0.004
1500	*	*	0.010	0.004
2000	*	*	0.009	0.004
2500	*	*	0.009	0.005

Table C.17. Effective Conductivity for Liquid Neutron Shield with 500°F Temperature Gradient

Avg. Temperature (°F)	56% Ethylene Glycol		Air	
	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)	Effective Conductivity Neutron Shield (Btu/hr-in-°F)	Effective Conductivity Expansion Tank (Btu/hr-in-°F)
250	*	*	0.016	0.006
260	*	*	0.016	0.006
270	*	*	0.016	0.006
280	*	*	0.016	0.006
290	*	*	0.016	0.006
300	*	*	0.015	0.006
310	*	*	0.015	0.006
320	*	*	0.015	0.006
330	*	*	0.015	0.006
340	*	*	0.015	0.006
350	*	*	0.015	0.006
351	*	*	0.015	0.006
400	*	*	0.015	0.006
500	*	*	0.014	0.006
600	*	*	0.014	0.005
700	*	*	0.014	0.005
800	*	*	0.013	0.005
1000	*	*	0.013	0.005
1200	*	*	0.012	0.005
1500	*	*	0.011	0.005
2000	*	*	0.011	0.004
2500	*	*	0.010	0.005

Table C.18. Emissivity Values for Radiation Heat Transfer

Component	**Material**	**Emissivity Before Fire**	**Emissivity During/After Fire**
Canister	stainless steel	0.36	0.36
Cask	stainless steel	0.36	0.36
Outer Neutron Shield		0.34	0.34
Inner Neutron Shield		0.34	0.34
Basket	stainless steel	0.36	0.36
Fuel Clad	zircaloy	0.8	0.8
Boral Plate	aluminum clad	0.55	0.55
Shell Interior	stainless steel	0.36	0.36
Cask Exterior	stainless steel	0.85	0.9
Tunnel/ISO	various		0.9

Appendix D

Boundary Conditions from FDS Simulation of Fully Ventilated Fire Scenario

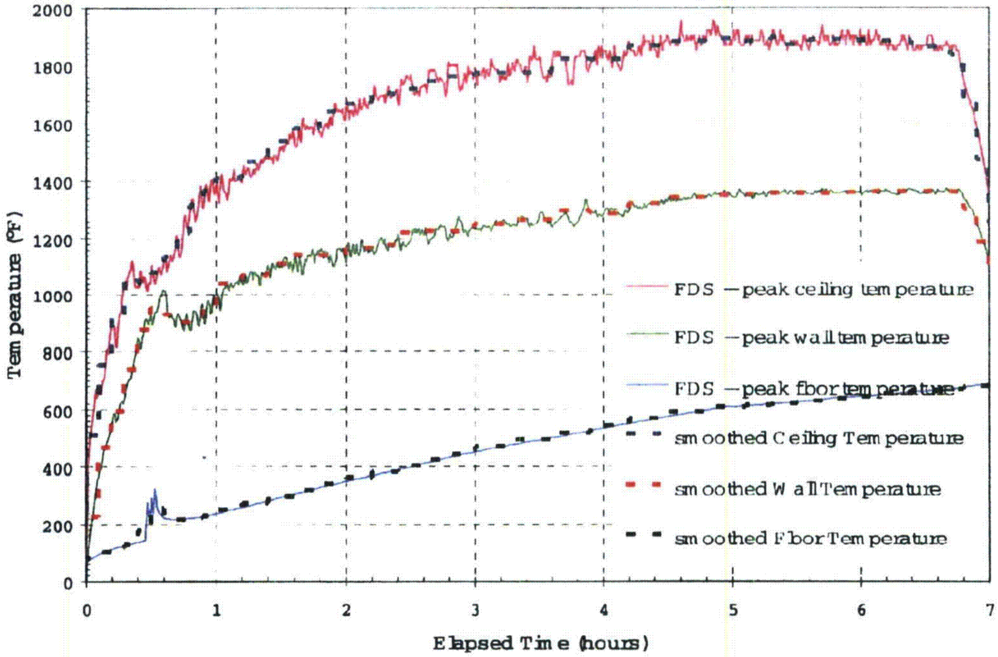

Figure D.1. Peak Surface Temperatures Calculated with FDS for Tunnel Ceiling, Wall, and Floor Regions at 20 meters from Fire Location during 7-hr Fire

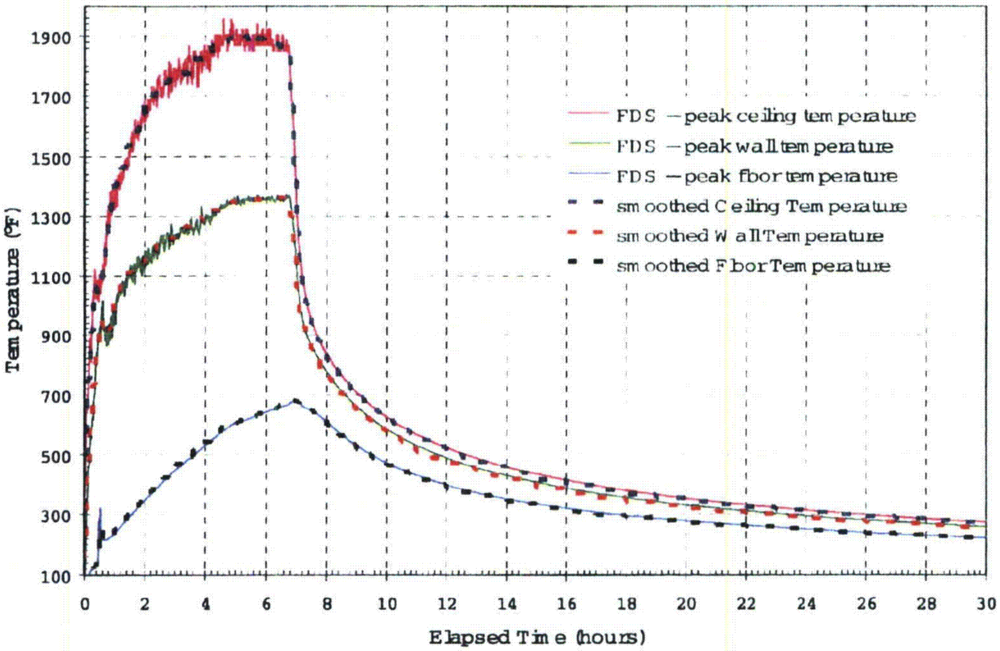

Figure D.2. Peak Surface Temperatures Calculated with FDS for Tunnel Ceiling, Wall, and Floor Regions at 20 meters from Fire Location during 7-hr Fire and 23-hr Cool Down

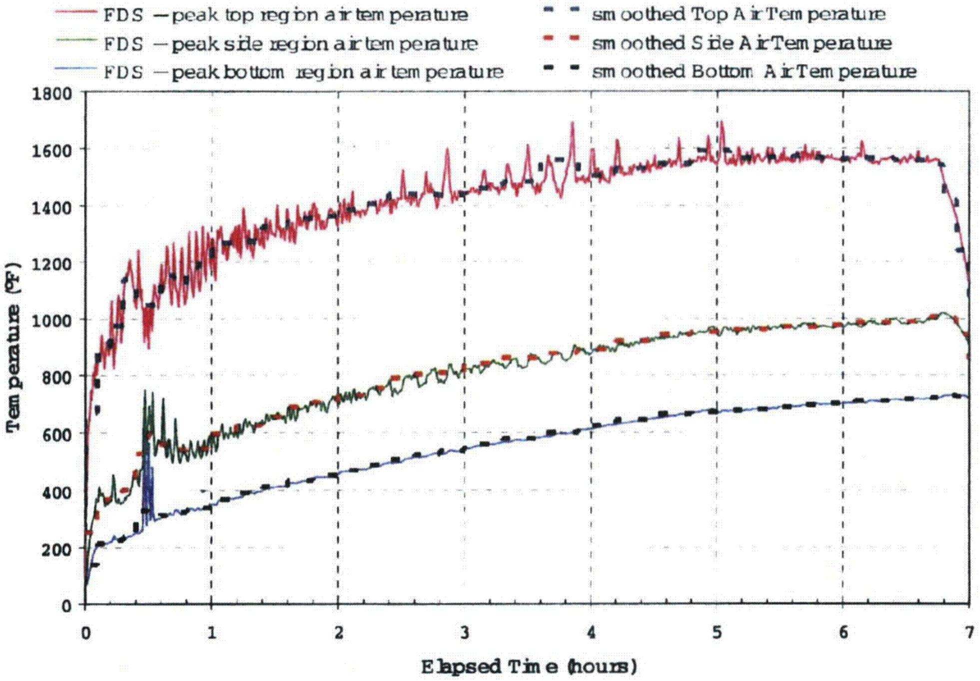

Figure D.3. Peak Air Temperatures Calculated with FDS for Top, Side, and Bottom Regions at 20 meters from Fire Location during 7-hr Fire

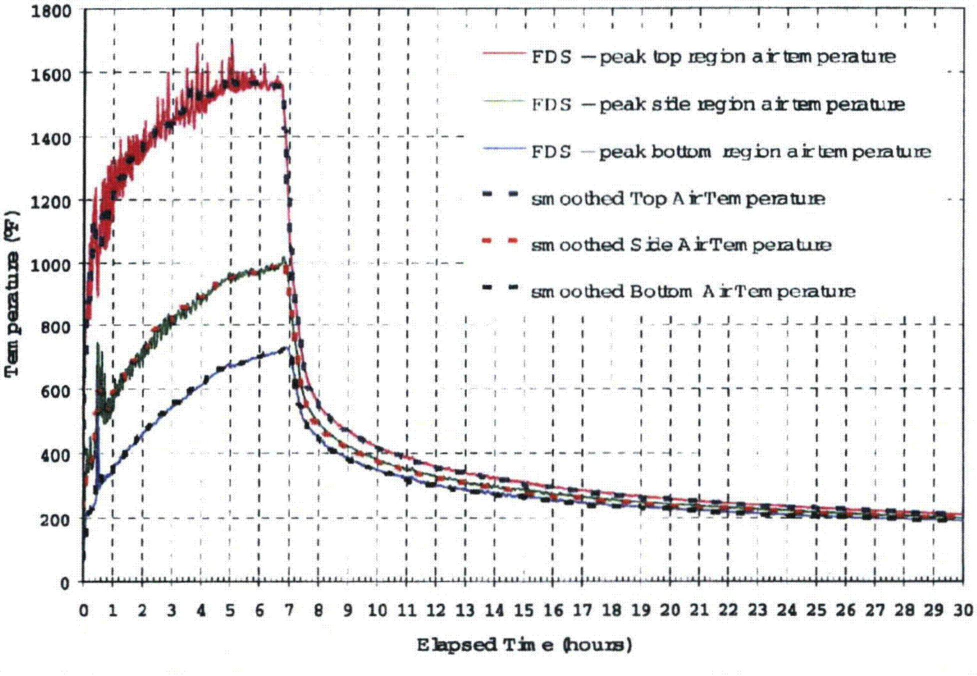

Figure D.4. Peak Air Temperatures Calculated with FDS for Top, Side, and Bottom Regions at 20 meters from Fire Location during 7-hr Fire and 23-hr Cool Down

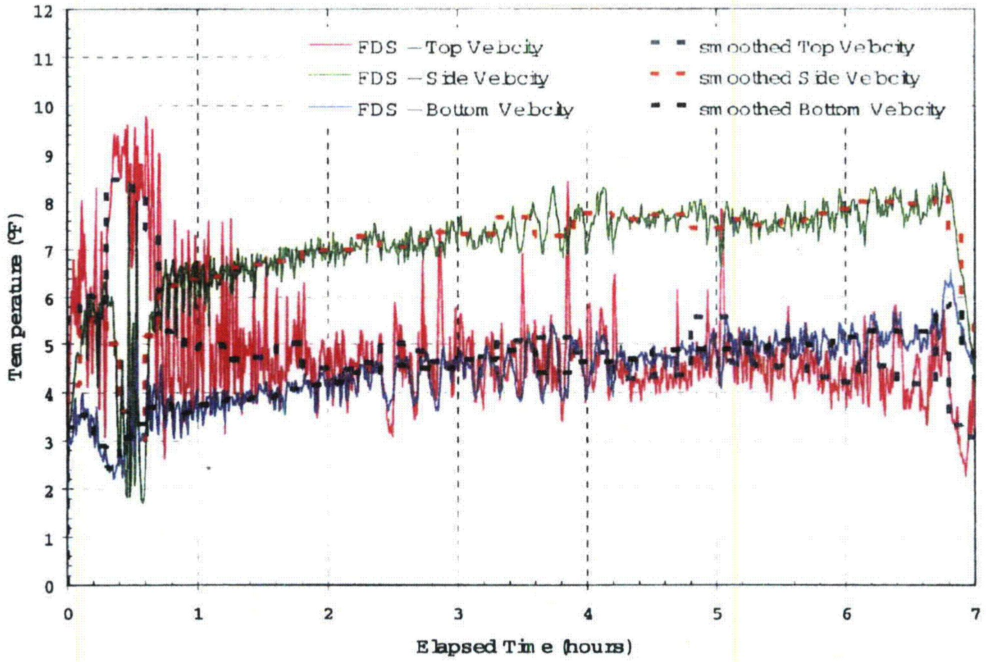

Figure D.5. Velocities at Peak Air Temperature Locations Calculated with FDS for Top, Side, and Bottom Regions at 20 meters from Fire Location during 7-hr Fire

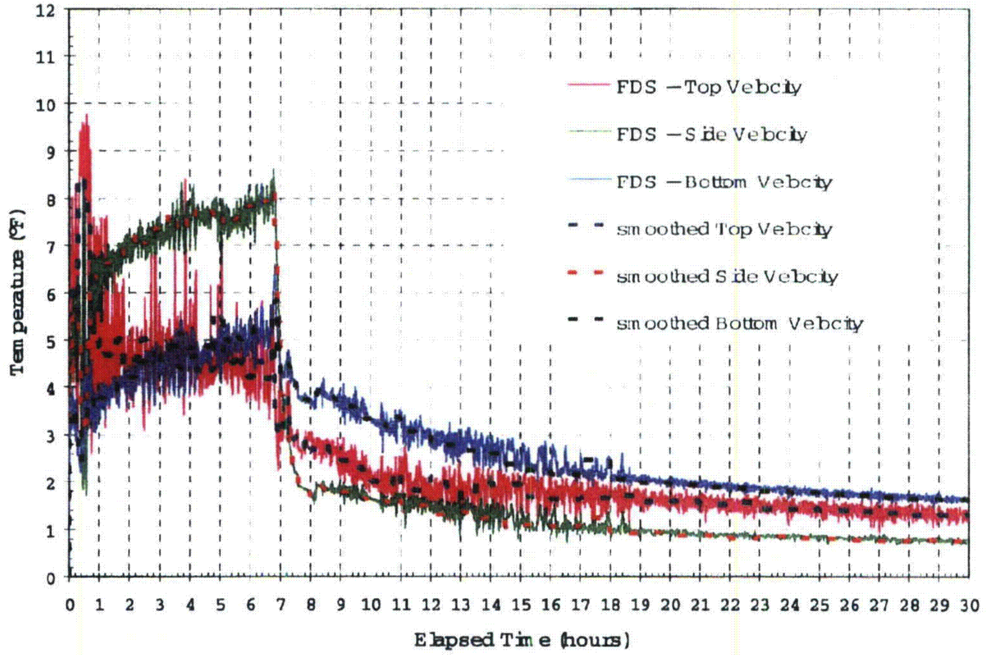

Figure D.6. Velocities at Peak Air Temperature Locations Calculated with FDS for Top, Side, and Bottom Regions at 20 meters from Fire during 7-hr Fire and 23-hr Cool Down

Appendix E

Blackbody Viewfactors for COBRA-SFS Model of TN-68 Package

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C TUNNEL.1=TOP  TUNNEL.2=SIDE  TUNNEL.3=BOTTOM
Cnode_1      node_2      Area*e*Bij  $       Bij       Bji
 CASK.101,   TUNNEL.1,   137.54      $      0.79418,0.00045040
 CASK.101,   TUNNEL.2,   25.477      $      0.14711,7.5761e-005
 CASK.101,   TUNNEL.3,   6.3683      $     0.036772,2.1839e-005
 CASK.102,   TUNNEL.1,   67.819      $      0.39161,0.00022209
 CASK.102,   TUNNEL.2,   87.635      $      0.50603,0.00026060
 CASK.102,   TUNNEL.3,   13.723      $     0.079241,4.7061e-005
 CASK.103,   TUNNEL.1,   12.951      $     0.074781,4.2410e-005
 CASK.103,   TUNNEL.2,   90.996      $      0.52544,0.00027059
 CASK.103,   TUNNEL.3,   65.443      $      0.37789,0.00022443
 CASK.104,   TUNNEL.1,   6.2714      $     0.036213,2.0538e-005
 CASK.104,   TUNNEL.2,   31.022      $      0.17913,9.2251e-005
 CASK.104,   TUNNEL.3,   132.32      $      0.76404,0.00045376
 CASK.201,   TUNNEL.1,   138.22      $      0.79812,0.00045264
 CASK.201,   TUNNEL.2,   24.978      $      0.14423,7.4277e-005
 CASK.201,   TUNNEL.3,   6.3135      $     0.036456,2.1651e-005
 CASK.202,   TUNNEL.1,   68.135      $      0.39343,0.00022313
 CASK.202,   TUNNEL.2,   87.392      $      0.50463,0.00025988
 CASK.202,   TUNNEL.3,   13.599      $     0.078527,4.6637e-005
 CASK.203,   TUNNEL.1,   12.937      $     0.074702,4.2365e-005
 CASK.203,   TUNNEL.2,   90.689      $      0.52367,0.00026968
 CASK.203,   TUNNEL.3,   65.701      $      0.37938,0.00022531
 CASK.204,   TUNNEL.1,   6.1691      $     0.035622,2.0203e-005
 CASK.204,   TUNNEL.2,   31.104      $      0.17961,9.2494e-005
 CASK.204,   TUNNEL.3,   132.31      $      0.76399,0.00045373
 CASK.301,   TUNNEL.1,   137.98      $      0.79676,0.00045187
 CASK.301,   TUNNEL.2,   25.169      $      0.14533,7.4844e-005
 CASK.301,   TUNNEL.3,   6.3354      $     0.036583,2.1726e-005
 CASK.302,   TUNNEL.1,   68.192      $      0.39376,0.00022331
 CASK.302,   TUNNEL.2,   87.312      $      0.50417,0.00025964
 CASK.302,   TUNNEL.3,   13.687      $     0.079031,4.6936e-005
 CASK.303,   TUNNEL.1,   12.880      $     0.074375,4.2180e-005
 CASK.303,   TUNNEL.2,   91.267      $      0.52700,0.00027140
 CASK.303,   TUNNEL.3,   65.225      $      0.37663,0.00022368
 CASK.304,   TUNNEL.1,   6.1435      $     0.035475,2.0119e-005
 CASK.304,   TUNNEL.2,   31.213      $      0.18024,9.2819e-005
 CASK.304,   TUNNEL.3,   132.07      $      0.76261,0.00045291
 CASK.401,   TUNNEL.1,   138.03      $      0.79704,0.00045202
 CASK.401,   TUNNEL.2,   25.096      $      0.14491,7.4627e-005
 CASK.401,   TUNNEL.3,   6.3204      $     0.036496,2.1675e-005
 CASK.402,   TUNNEL.1,   68.295      $      0.39436,0.00022365
 CASK.402,   TUNNEL.2,   87.210      $      0.50358,0.00025934
 CASK.402,   TUNNEL.3,   13.632      $     0.078717,4.6750e-005
 CASK.403,   TUNNEL.1,   13.054      $     0.075376,4.2748e-005
 CASK.403,   TUNNEL.2,   90.720      $      0.52385,0.00026977
 CASK.403,   TUNNEL.3,   65.460      $      0.37799,0.00022448
 CASK.404,   TUNNEL.1,   6.0468      $     0.034916,1.9802e-005
 CASK.404,   TUNNEL.2,   30.945      $      0.17868,9.2020e-005
 CASK.404,   TUNNEL.3,   132.46      $      0.76484,0.00045424
 CASK.501,   TUNNEL.1,   138.12      $      0.79752,0.00045230
 CASK.501,   TUNNEL.2,   25.031      $      0.14454,7.4434e-005
 CASK.501,   TUNNEL.3,   6.3279      $     0.036539,2.1701e-005
 CASK.502,   TUNNEL.1,   68.608      $      0.39616,0.00022468
 CASK.502,   TUNNEL.2,   86.890      $      0.50173,0.00025838
 CASK.502,   TUNNEL.3,   13.693      $     0.079069,4.6959e-005
 CASK.503,   TUNNEL.1,   12.946      $     0.074757,4.2397e-005
 CASK.503,   TUNNEL.2,   91.115      $      0.52613,0.00027095
 CASK.503,   TUNNEL.3,   65.179      $      0.37636,0.00022352
 CASK.504,   TUNNEL.1,   5.9701      $     0.034474,1.9551e-005
 CASK.504,   TUNNEL.2,   30.931      $      0.17860,9.1979e-005
 CASK.504,   TUNNEL.3,   132.45      $      0.76480,0.00045422
 CASK.601,   TUNNEL.1,   138.18      $      0.79788,0.00045250
 CASK.601,   TUNNEL.2,   24.944      $      0.14403,7.4176e-005
 CASK.601,   TUNNEL.3,   6.2188      $     0.035909,2.1326e-005
 CASK.602,   TUNNEL.1,   68.688      $      0.39663,0.00022494
 CASK.602,   TUNNEL.2,   86.987      $      0.50229,0.00025867
 CASK.602,   TUNNEL.3,   13.491      $     0.077899,4.6264e-005
 CASK.603,   TUNNEL.1,   13.037      $     0.075278,4.2692e-005
 CASK.603,   TUNNEL.2,   91.088      $      0.52597,0.00027087
 CASK.603,   TUNNEL.3,   65.088      $      0.37584,0.00022321
 CASK.604,   TUNNEL.1,   5.9256      $     0.034216,1.9405e-005
 CASK.604,   TUNNEL.2,   31.061      $      0.17936,9.2367e-005
 CASK.604,   TUNNEL.3,   132.28      $      0.76385,0.00045365
 CASK.701,   TUNNEL.1,   138.29      $      0.79854,0.00045288
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  CASK.701,  TUNNEL.2,  24.847     $    0.14347,7.3886e-005
  CASK.701,  TUNNEL.3,  6.2483     $   0.036080,2.1428e-005
  CASK.702,  TUNNEL.1,  68.225     $    0.39395,0.00022342
  CASK.702,  TUNNEL.2,  87.042     $    0.50261,0.00025884
  CASK.702,  TUNNEL.3,  13.824     $   0.079826,4.7408e-005
  CASK.703,  TUNNEL.1,  13.006     $   0.075103,4.2593e-005
  CASK.703,  TUNNEL.2,  90.921     $    0.52501,0.00027037
  CASK.703,  TUNNEL.3,  65.304     $    0.37709,0.00022395
  CASK.704,  TUNNEL.1,  5.8582     $   0.033827,1.9184e-005
  CASK.704,  TUNNEL.2,  31.026     $    0.17916,9.2263e-005
  CASK.704,  TUNNEL.3,  132.40     $    0.76454,0.00045406
  CASK.801,  TUNNEL.1,  138.10     $    0.79744,0.00045225
  CASK.801,  TUNNEL.2,  25.102     $    0.14495,7.4647e-005
  CASK.801,  TUNNEL.3,  6.2315     $   0.035983,2.1370e-005
  CASK.802,  TUNNEL.1,  67.889     $    0.39201,0.00022232
  CASK.802,  TUNNEL.2,  87.307     $    0.50414,0.00025963
  CASK.802,  TUNNEL.3,  13.869     $   0.080083,4.7561e-005
  CASK.803,  TUNNEL.1,  12.847     $   0.074182,4.2071e-005
  CASK.803,  TUNNEL.2,  90.805     $    0.52434,0.00027003
  CASK.803,  TUNNEL.3,  65.535     $    0.37842,0.00022474
  CASK.804,  TUNNEL.1,  5.7617     $   0.033270,1.8868e-005
  CASK.804,  TUNNEL.2,  31.064     $    0.17937,9.2374e-005
  CASK.804,  TUNNEL.3,  132.36     $    0.76430,0.00045391
  CASK.901,  TUNNEL.1,  138.13     $    0.79764,0.00045236
  CASK.901,  TUNNEL.2,  25.128     $    0.14510,7.4724e-005
  CASK.901,  TUNNEL.3,  6.2207     $   0.035921,2.1333e-005
  CASK.902,  TUNNEL.1,  68.091     $    0.39318,0.00022298
  CASK.902,  TUNNEL.2,  87.277     $    0.50396,0.00025953
  CASK.902,  TUNNEL.3,  13.700     $   0.079107,4.6981e-005
  CASK.903,  TUNNEL.1,  12.952     $   0.074789,4.2415e-005
  CASK.903,  TUNNEL.2,  90.982     $    0.52536,0.00027055
  CASK.903,  TUNNEL.3,  65.132     $    0.37609,0.00022336
  CASK.904,  TUNNEL.1,  5.7522     $   0.033215,1.8837e-005
  CASK.904,  TUNNEL.2,  31.124     $    0.17972,9.2553e-005
  CASK.904,  TUNNEL.3,  132.31     $    0.76400,0.00045374
 CASK.1001,  TUNNEL.1,  137.84     $    0.79593,0.00045139
 CASK.1001,  TUNNEL.2,  25.412     $    0.14674,7.5569e-005
 CASK.1001,  TUNNEL.3,  6.2311     $   0.035981,2.1369e-005
 CASK.1002,  TUNNEL.1,  68.564     $    0.39591,0.00022453
 CASK.1002,  TUNNEL.2,  86.856     $    0.50153,0.00025828
 CASK.1002,  TUNNEL.3,  13.655     $   0.078849,4.6828e-005
 CASK.1003,  TUNNEL.1,  12.716     $   0.073426,4.1642e-005
 CASK.1003,  TUNNEL.2,  90.924     $    0.52502,0.00027038
 CASK.1003,  TUNNEL.3,  65.500     $    0.37822,0.00022462
 CASK.1004,  TUNNEL.1,  5.6573     $   0.032667,1.8526e-005
 CASK.1004,  TUNNEL.2,  31.197     $    0.18014,9.2770e-005
 CASK.1004,  TUNNEL.3,  132.27     $    0.76378,0.00045361
 CASK.1101,  TUNNEL.1,  138.17     $    0.79782,0.00045247
 CASK.1101,  TUNNEL.2,  25.006     $    0.14440,7.4362e-005
 CASK.1101,  TUNNEL.3,  6.1872     $   0.035727,2.1218e-005
 CASK.1102,  TUNNEL.1,  68.446     $    0.39523,0.00022415
 CASK.1102,  TUNNEL.2,  87.049     $    0.50265,0.00025886
 CASK.1102,  TUNNEL.3,  13.505     $   0.077980,4.6312e-005
 CASK.1103,  TUNNEL.1,  12.985     $   0.074982,4.2524e-005
 CASK.1103,  TUNNEL.2,  91.145     $    0.52630,0.00027104
 CASK.1103,  TUNNEL.3,  65.061     $    0.37568,0.00022312
 CASK.1104,  TUNNEL.1,  5.6898     $   0.032855,1.8633e-005
 CASK.1104,  TUNNEL.2,  30.943     $    0.17868,9.2016e-005
 CASK.1104,  TUNNEL.3,  132.40     $    0.76453,0.00045405
 CASK.1201,  TUNNEL.1,  137.88     $    0.79616,0.00045153
 CASK.1201,  TUNNEL.2,  25.412     $    0.14674,7.5567e-005
 CASK.1201,  TUNNEL.3,  6.1750     $   0.035657,2.1176e-005
 CASK.1202,  TUNNEL.1,  67.908     $    0.39212,0.00022238
 CASK.1202,  TUNNEL.2,  87.439     $    0.50490,0.00026002
 CASK.1202,  TUNNEL.3,  13.717     $   0.079208,4.7041e-005
 CASK.1203,  TUNNEL.1,  12.998     $   0.075055,4.2566e-005
 CASK.1203,  TUNNEL.2,  90.706     $    0.52377,0.00026973
 CASK.1203,  TUNNEL.3,  65.409     $    0.37769,0.00022431
 CASK.1204,  TUNNEL.1,  5.5919     $   0.032289,1.8312e-005
 CASK.1204,  TUNNEL.2,  31.129     $    0.17975,9.2567e-005
 CASK.1204,  TUNNEL.3,  132.27     $    0.76380,0.00045362
 CASK.1301,  TUNNEL.1,  138.25     $    0.79830,0.00045274
 CASK.1301,  TUNNEL.2,  24.993     $    0.14432,7.4320e-005
 CASK.1301,  TUNNEL.3,  6.1782     $   0.035675,2.1187e-005
 CASK.1302,  TUNNEL.1,  68.155     $    0.39355,0.00022319
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CASK.1302,  TUNNEL.2,  87.155      $       0.50326,0.00025917
CASK.1302,  TUNNEL.3,  13.671      $      0.078943,4.6884e-005
CASK.1303,  TUNNEL.1,  12.737      $      0.073546,4.1710e-005
CASK.1303,  TUNNEL.2,  91.130      $       0.52622,0.00027099
CASK.1303,  TUNNEL.3,  65.245      $       0.37674,0.00022375
CASK.1304,  TUNNEL.1,  5.6600      $      0.032683,1.8535e-005
CASK.1304,  TUNNEL.2,  31.047      $       0.17928,9.2325e-005
CASK.1304,  TUNNEL.3,  132.30      $       0.76396,0.00045372
CASK.1401,  TUNNEL.1,  138.04      $       0.79707,0.00045204
CASK.1401,  TUNNEL.2,  25.121      $       0.14506,7.4703e-005
CASK.1401,  TUNNEL.3,  6.2019      $      0.035812,2.1269e-005
CASK.1402,  TUNNEL.1,  68.195      $       0.39378,0.00022332
CASK.1402,  TUNNEL.2,  87.073      $       0.50279,0.00025893
CASK.1402,  TUNNEL.3,  13.807      $      0.079729,4.7351e-005
CASK.1403,  TUNNEL.1,  12.669      $      0.073153,4.1487e-005
CASK.1403,  TUNNEL.2,  91.365      $       0.52757,0.00027169
CASK.1403,  TUNNEL.3,  65.069      $       0.37573,0.00022314
CASK.1404,  TUNNEL.1,  5.5888      $      0.032272,1.8302e-005
CASK.1404,  TUNNEL.2,  31.163      $       0.17994,9.2668e-005
CASK.1404,  TUNNEL.3,  132.22      $       0.76345,0.00045341
CASK.1501,  TUNNEL.1,  138.17      $       0.79785,0.00045249
CASK.1501,  TUNNEL.2,  25.075      $       0.14479,7.4565e-005
CASK.1501,  TUNNEL.3,  6.1911      $      0.035749,2.1231e-005
CASK.1502,  TUNNEL.1,  68.139      $       0.39346,0.00022314
CASK.1502,  TUNNEL.2,  87.197      $       0.50350,0.00025930
CASK.1502,  TUNNEL.3,  13.646      $      0.078797,4.6798e-005
CASK.1503,  TUNNEL.1,  12.784      $      0.073818,4.1864e-005
CASK.1503,  TUNNEL.2,  91.009      $       0.52552,0.00027063
CASK.1503,  TUNNEL.3,  65.230      $       0.37666,0.00022370
CASK.1504,  TUNNEL.1,  5.6144      $      0.032419,1.8386e-005
CASK.1504,  TUNNEL.2,  31.011      $       0.17907,9.2217e-005
CASK.1504,  TUNNEL.3,  132.37      $       0.76434,0.00045394
CASK.1601,  TUNNEL.1,  138.38      $       0.79906,0.00045317
CASK.1601,  TUNNEL.2,  24.861      $       0.14355,7.3928e-005
CASK.1601,  TUNNEL.3,  6.1468      $      0.035493,2.1079e-005
CASK.1602,  TUNNEL.1,  68.267      $       0.39420,0.00022356
CASK.1602,  TUNNEL.2,  87.153      $       0.50325,0.00025917
CASK.1602,  TUNNEL.3,  13.587      $      0.078457,4.6595e-005
CASK.1603,  TUNNEL.1,  12.646      $      0.073025,4.1414e-005
CASK.1603,  TUNNEL.2,  90.521      $       0.52270,0.00026918
CASK.1603,  TUNNEL.3,  65.886      $       0.38045,0.00022595
CASK.1604,  TUNNEL.1,  5.5843      $      0.032245,1.8287e-005
CASK.1604,  TUNNEL.2,  30.824      $       0.17799,9.1661e-005
CASK.1604,  TUNNEL.3,  132.56      $       0.76547,0.00045461
CASK.1701,  TUNNEL.1,  137.89      $       0.79623,0.00045156
CASK.1701,  TUNNEL.2,  25.339      $       0.14632,7.5351e-005
CASK.1701,  TUNNEL.3,  6.1499      $      0.035511,2.1090e-005
CASK.1702,  TUNNEL.1,  68.793      $       0.39723,0.00022528
CASK.1702,  TUNNEL.2,  86.707      $       0.50067,0.00025784
CASK.1702,  TUNNEL.3,  13.529      $      0.078123,4.6397e-005
CASK.1703,  TUNNEL.1,  12.704      $      0.073359,4.1604e-005
CASK.1703,  TUNNEL.2,  91.173      $       0.52646,0.00027112
CASK.1703,  TUNNEL.3,  65.193      $       0.37645,0.00022357
CASK.1704,  TUNNEL.1,  5.5697      $      0.032161,1.8240e-005
CASK.1704,  TUNNEL.2,  31.366      $       0.18112,9.3274e-005
CASK.1704,  TUNNEL.3,  132.07      $       0.76260,0.00045290
CASK.1801,  TUNNEL.1,  138.30      $       0.79856,0.00045289
CASK.1801,  TUNNEL.2,  24.981      $       0.14425,7.4286e-005
CASK.1801,  TUNNEL.3,  6.1334      $      0.035416,2.1034e-005
CASK.1802,  TUNNEL.1,  68.068      $       0.39304,0.00022291
CASK.1802,  TUNNEL.2,  87.271      $       0.50393,0.00025952
CASK.1802,  TUNNEL.3,  13.649      $      0.078815,4.6808e-005
CASK.1803,  TUNNEL.1,  12.694      $      0.073297,4.1569e-005
CASK.1803,  TUNNEL.2,  91.352      $       0.52750,0.00027165
CASK.1803,  TUNNEL.3,  65.097      $       0.37589,0.00022324
CASK.1804,  TUNNEL.1,  5.5500      $      0.032047,1.8175e-005
CASK.1804,  TUNNEL.2,  31.344      $       0.18099,9.3208e-005
CASK.1804,  TUNNEL.3,  132.06      $       0.76255,0.00045287
CASK.1901,  TUNNEL.1,  138.28      $       0.79848,0.00045284
CASK.1901,  TUNNEL.2,  24.841      $       0.14344,7.3870e-005
CASK.1901,  TUNNEL.3,  6.1732      $      0.035646,2.1170e-005
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CASK.2101,   TUNNEL.1,  137.99       $      0.79682,0.00045190
CASK.2101,   TUNNEL.2,  25.223       $      0.14564,7.5005e-005
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CASK.2102,   TUNNEL.1,  68.122       $      0.39336,0.00022308
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CASK.2901,   TUNNEL.1,   137.95      $      0.79655,0.00045175
CASK.2901,   TUNNEL.2,   25.208      $      0.14556,7.4961e-005
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CASK.2902,   TUNNEL.3,   13.756      $     0.079430,4.7173e-005
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CASK.2904,   TUNNEL.1,   6.0330      $     0.034837,1.9757e-005
CASK.2904,   TUNNEL.2,   31.507      $      0.18193,9.3691e-005
CASK.2904,   TUNNEL.3,   131.85      $      0.76133,0.00045215
CASK.3001,   TUNNEL.1,   137.97      $      0.79670,0.00045183
CASK.3001,   TUNNEL.2,   25.049      $      0.14464,7.4489e-005
CASK.3001,   TUNNEL.3,   6.3339      $     0.036574,2.1721e-005
CASK.3002,   TUNNEL.1,   68.734      $      0.39689,0.00022509
CASK.3002,   TUNNEL.2,   86.655      $      0.50038,0.00025769
CASK.3002,   TUNNEL.3,   13.815      $     0.079771,4.7376e-005
CASK.3003,   TUNNEL.1,   12.791      $     0.073857,4.1886e-005
CASK.3003,   TUNNEL.2,   91.080      $      0.52592,0.00027084
CASK.3003,   TUNNEL.3,   65.432      $      0.37783,0.00022439
CASK.3004,   TUNNEL.1,   6.1398      $     0.035453,2.0106e-005
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CASK.3004,   TUNNEL.3,   132.27      $      0.76378,0.00045361
CASK.3101,   TUNNEL.1,   137.70      $      0.79510,0.00045093
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CASK.3102,   TUNNEL.2,   87.177      $      0.50339,0.00025924
CASK.3102,   TUNNEL.3,   13.703      $     0.079126,4.6993e-005
CASK.3103,   TUNNEL.1,   12.902      $     0.074502,4.2252e-005
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CASK.3104,   TUNNEL.1,   6.2118      $     0.035869,2.0342e-005
CASK.3104,   TUNNEL.2,   30.914      $      0.17851,9.1928e-005
CASK.3104,   TUNNEL.3,   132.43      $      0.76470,0.00045415
CASK.3201,   TUNNEL.1,   138.23      $      0.79819,0.00045268
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CASK.3202,  TUNNEL.3,  13.866      $    0.080068,4.7552e-005
CASK.3203,  TUNNEL.1,  13.070      $    0.075471,4.2802e-005
CASK.3203,  TUNNEL.2,  90.901      $     0.52489,0.00027031
CASK.3203,  TUNNEL.3,  65.388      $     0.37757,0.00022424
CASK.3204,  TUNNEL.1,  6.2751      $    0.036235,2.0550e-005
CASK.3204,  TUNNEL.2,  31.217      $     0.18026,9.2829e-005
CASK.3204,  TUNNEL.3,  132.12      $     0.76292,0.00045309
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Appendix F

HOLTEC HI-STAR 100 Component Temperature Distributions

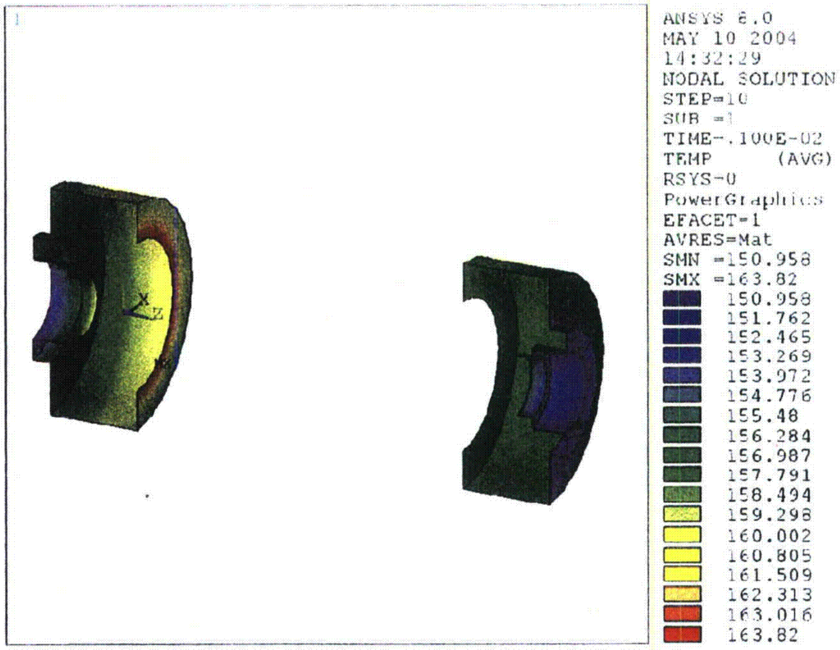

Figure F.1. Impact Limiter Skin Temperature Distribution - Normal Transport Conditions.

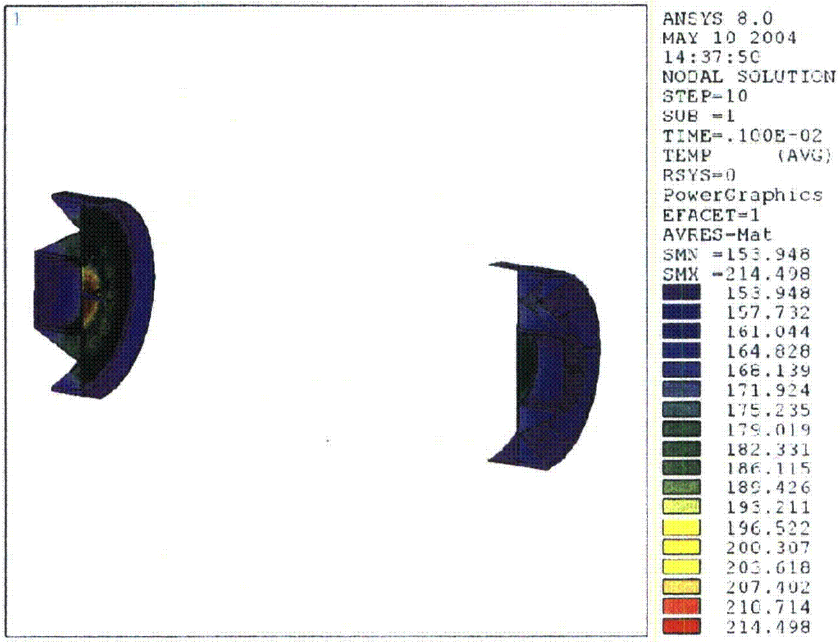

Figure F.2. Impact Limiter Structure Temperature Distribution - Normal Transport Conditions.

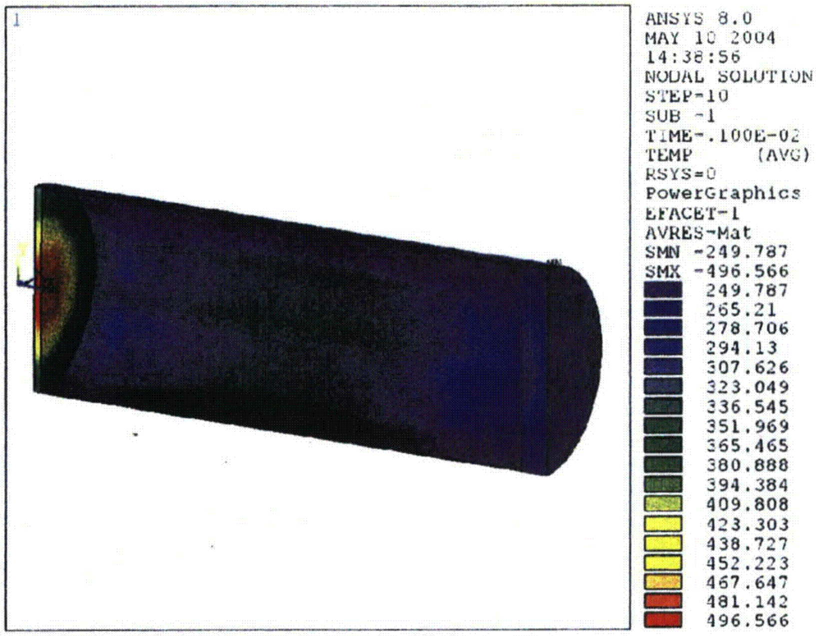

Figure F.3. Canister Shell Temperature Distribution - Normal Transport Conditions.

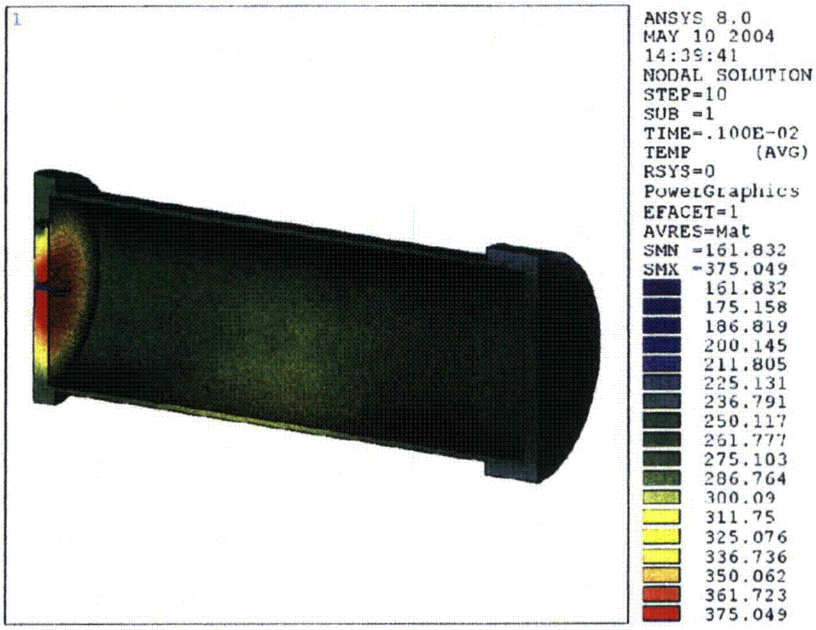

Figure F.4. Cask Inner Shell (Primary Containment Boundary) Temperature Distribution - Normal Transport Conditions.

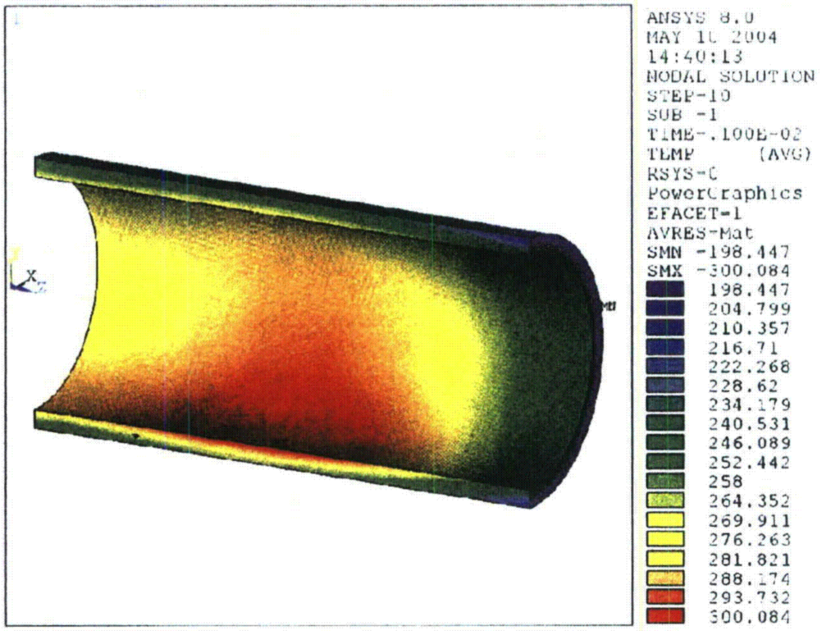

Figure F.5. Gamma Shield Temperature Distribution - Normal Transport Conditions.

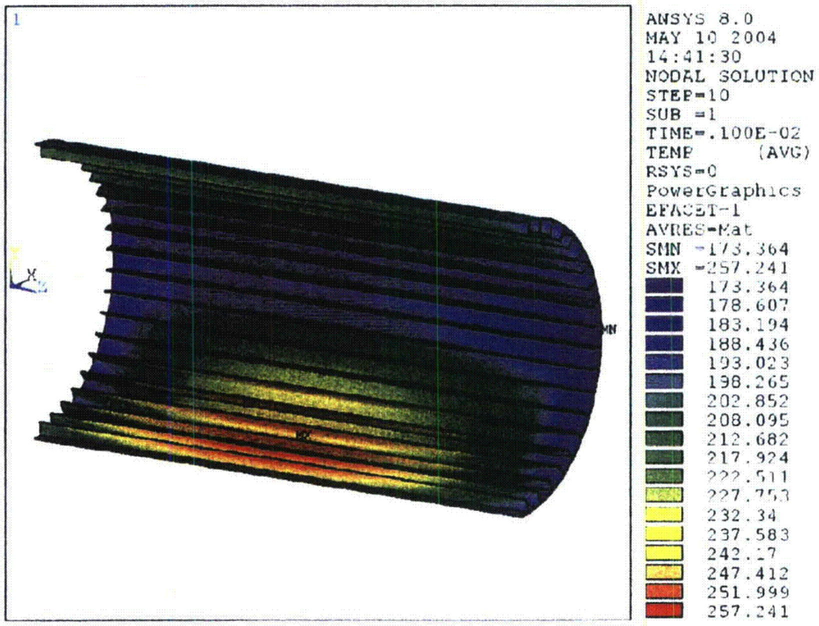

Figure F.6. Fin Structure Temperature Distribution - Normal Transport Conditions.

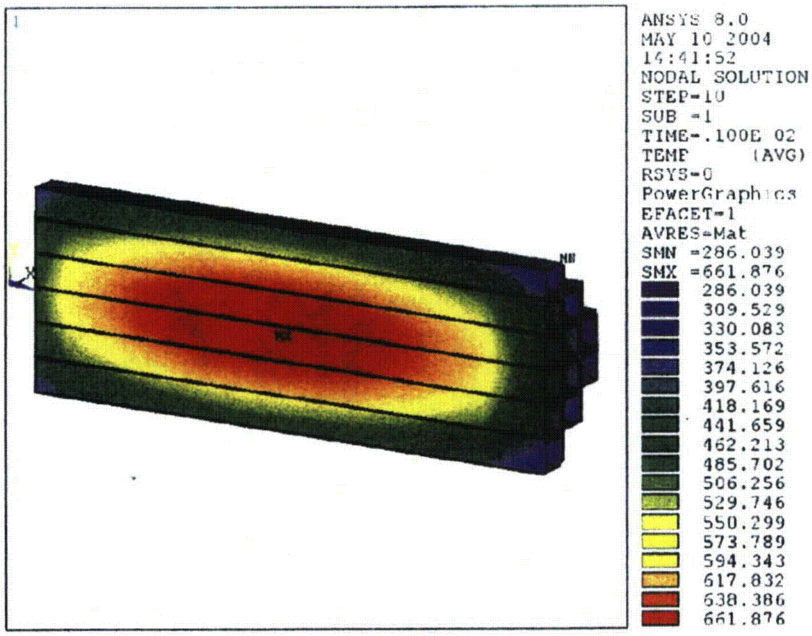

Figure F.7. Basket Axial Temperature Distribution - Normal Transport Conditions.

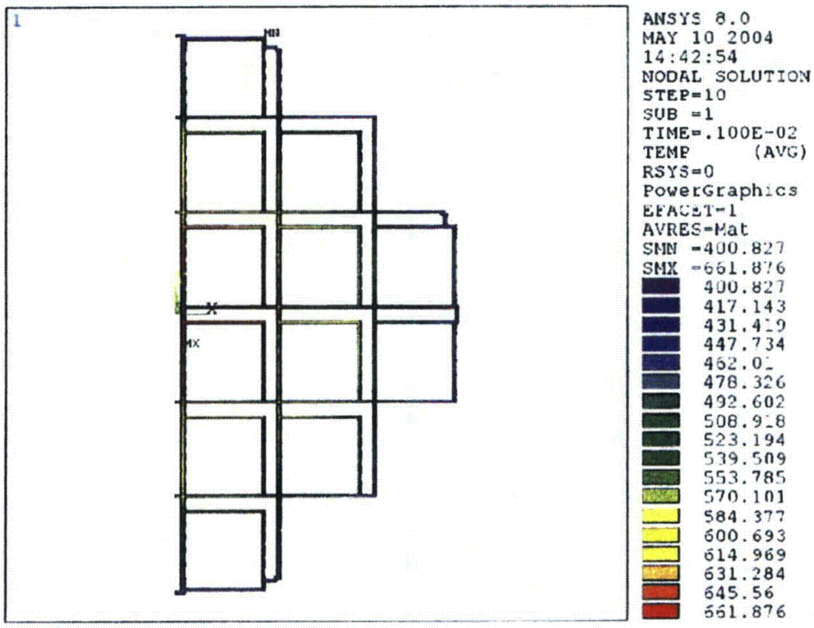

Figure F.8. Basket Radial Temperature Distribution - Normal Transport Conditions.

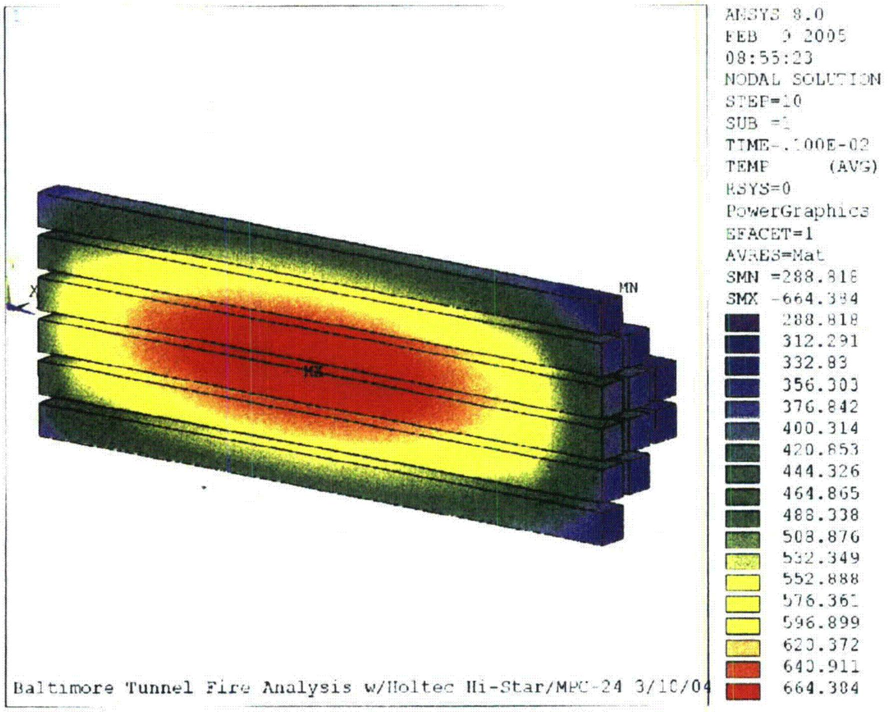

Figure F.9. Spent Fuel Axial Temperature Distribution - Normal Transport Conditions.

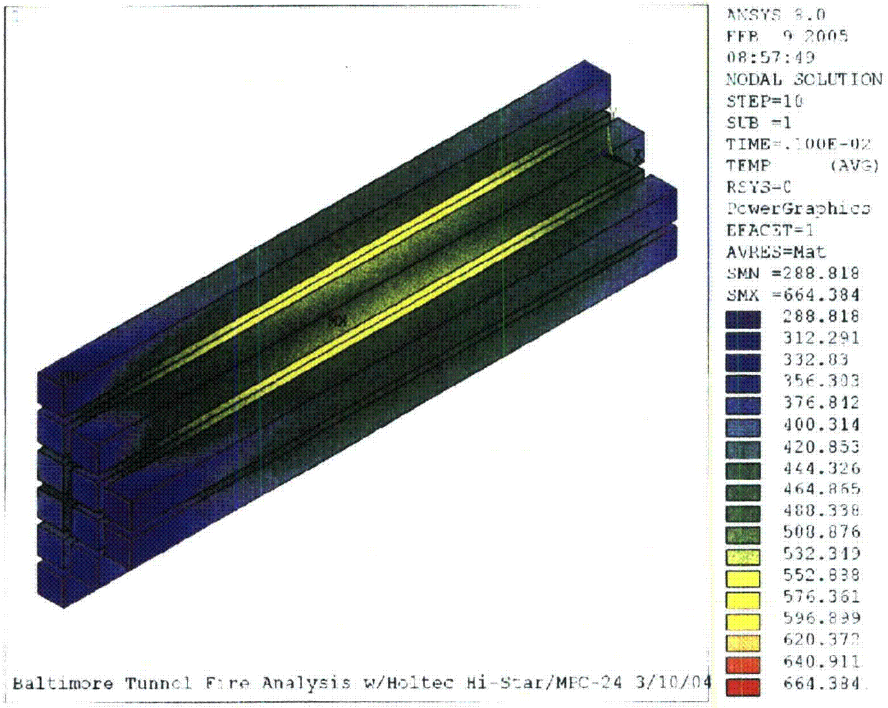

Figure F.10. Spent Fuel Axial Temperature Distribution - Normal Transport Conditions.

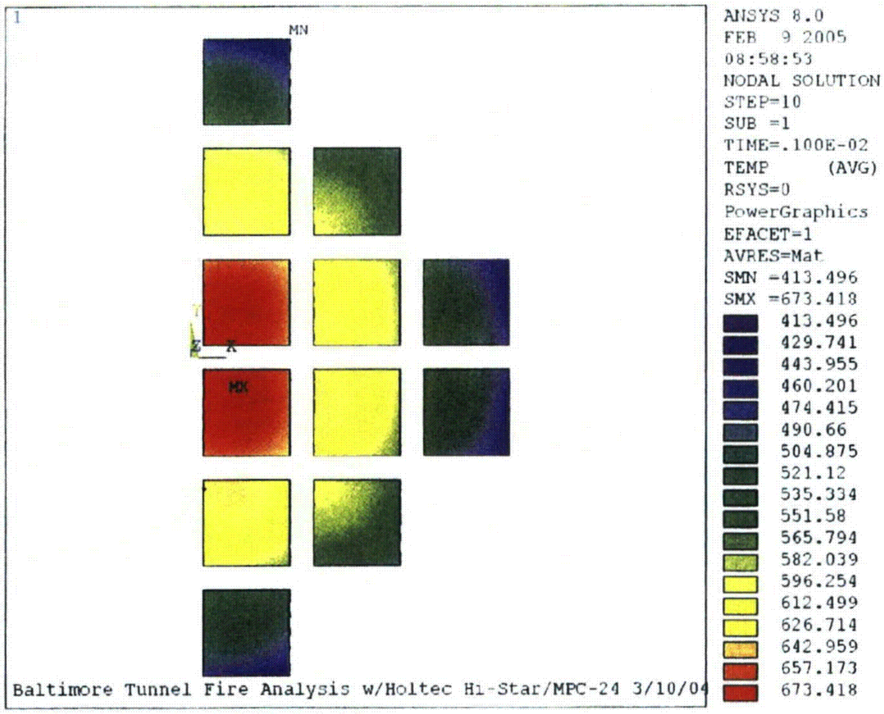

Figure F.11. Spent Fuel Radial Temperature Distribution - Normal Transport Conditions.

APPENDIX G

Comments and Responses from Public Posting in the Federal Register

Comments on NUREG/CR-6886 were solicited via a Federal Register Notice dated September 16, 2005. A second Federal Register Notice was posted on November 30, 2005, extending the comment period on this document to December 30, 2005. The NRC received comments from a diverse group of external stakeholders, consisting of

Northeast High Level Radioactive Waste Transportation Project
Brotherhood of Locomotive Engineers and Trainmen
Agency for Nuclear Projects, State of Nevada
William Rothman, M.D. (private citizen)

Comments ranged from concerns about the potential consequences of the effects of the fire transient on spent fuel transportation packages to comments that raised questions related to the basis for the staff's analysis. A revised version of this document (NUREG/CR-6886, Revision 1) has been developed, which includes additional discussion addressing the issues raised in these comments, an expanded level of detail in the explanation of the analysis methodology, and additional analysis of the potential consequences of the accident scenario. The comments[19] submitted by external stakeholders and the staff's responses to those comments are summarized in the following table.

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[19] Some comments have been condensed slightly to remove redundancies or edited to correct typographical errors, without omitting any relevant point of the comment. Full text of the original comments, as submitted to the NRC point of contact for this document, can be obtained from the Agencywide Documents Access and Management System (ADAMS) under the accession number **ML062340334**.

Summary of Comments and Responses from Public Posting on the Federal Register (9/16/2005 through 12/30/2005) of NUREG/CR-6886

Spent Fuel Transportation Package Response to the Baltimore Tunnel Fire Scenario

No.	Comment	Response
1	On page 5.1 the statement is made that 66 ft. downstream from the fire source is the shortest possible distance between the fire center and an SNF package because of the existence of a buffer car. This assumption seems problematic: even in the Baltimore Tunnel and certainly in wider tunnels with more than one track – it seems possible that the cask car and a buffer car could become uncoupled and slide past each other, that the buffer car could override or be overridden by the package car or that the derailment could realign the cars in such a way that the minimum distance between the fire Center and the package could be only a few feet.	The 66-ft (20-m) location was chosen as a reasonable estimate of where the package could have been located in this particular fire, based on Federal regulations issued by the Department of Transportation (DOT). DOT regulations, in 49CFR174.85, require very specifically defined spacing between rail cars carrying hazardous materials of any kind, including flammable liquids and radioactive materials. Typical requirements specify that a rail car carrying radioactive material must be separated from cars carrying other hazardous material by at least one buffer car. Therefore, the package was placed in a realistic location for this particular accident, not a 'worst possible location' for any tunnel fire scenario. Additions to Chapter 5 address this issue in an expanded discussion of the fire scenario, the configuration of the derailed train cars, and the modeling approach. Additions to Chapter 1 evaluate the Baltimore tunnel fire in relation to the frequency and severity of rail transportation accidents involving hazardous material and severe fires.

No.	Comment	Response
2	The study assumes that the package remains horizontal with one end facing the fire source. It states that this orientation results in the maximum possible exposure and in the least post-fire free convection cooling. While I do not doubt that that is true, it would seem that there should be some discussion or study of an inclined or vertical package particularly, as I believe is pointed out later, because of the vertical temperature distributions both in the air and on the tunnel walls. (Would the seals in a vertical [c]ask where the end is near the heated ceiling of the Tunnel – or sitting just above a pool of flammable liquid – exceed rated service temperatures sooner than in the assumed position?)	The position and orientation of the package within the tunnel was selected to maximize heat input to the package from convection and radiation heat transfer. Peak gas and tunnel surface temperatures were used as boundary conditions on the package surface, as a conservative estimate of the distributed temperature gradients the package would actually see within the tunnel environment at any orientation. This is of particular importance in terms of maximizing heat input to the seals, because the package ends (and therefore the seals) are covered by the impact limiters, which shield the seal region from direct convection and thermal radiation from the tunnel environment. The heat input to the package side governs the rate of heat up of the seals, rather than heat input to the package ends, since the seals heat up primarily because of conduction from the package side. Additions to Chapters 5 and 6, which expand the discussion of the modeling approach, include a review of the conservative assumptions underlying the selection of the package orientation, location relative to the fire, and boundary conditions.
3	on page 5.7, the analysis assumed that the center axis of the package would be 8.2 ft. above the Tunnel floor. ... it is not obvious that it is a worst-case position .... (While I understand from the comment in the first numbered paragraph of section 6.1 that the peak gas temperature at the top of the Tunnel was used as the ambient temperature for active heat transfer to the upper surfaces of the packages, it is not clear to me that this is equivalent to assuming that the package itself were higher in the Tunnel.)	Using the peak gas temperatures for the boundary conditions is equivalent to assuming the package is located at that corresponding position in the tunnel. The 'worst case' for convection would be to assume that the package is positioned near the tunnel ceiling, and the peak air temperature is seen by all package surfaces; however, radiation view factors to the tunnel walls and floor would be attenuated. Since radiation heat transfer is at least an order of magnitude greater than convection, this position would not produce the worst heat transfer conditions for the package. The 'worst case' for radiation assumes the package is oriented horizontally, near the center of the tunnel, so that it has the most direct radiation view factors on all surfaces, particularly the sides of the package. This orientation is used in the analysis, and is arguably the 'most adverse orientation' for heat transfer during the fire and in the post-fire cool down. Additions to Chapters 5 and 6 expand the discussion of the modeling approach, including discussion of the conservative assumptions underlying the selection of the package orientation, location relative to the fire, and boundary conditions.

No.	Comment	Response
4	regarding the use of a seven-hour fire [based on the predictions of the NIST Fire Dynamics Simulator code calculations for the tunnel fire], ...there should be some discussion of both the confidence of the 7-hr FDS prediction and of the [potential consequences] of a fire lasting 10 or more hours.	Seven hours is an extremely conservative estimate of the possible duration of the Baltimore tunnel fire. Based on known facts about the Baltimore tunnel fire (e.g., from NTSB accounts of the accident and testimony of emergency responders at the scene), the most severe portion of the Howard Street tunnel fire lasted approximately 3 hours. Sensitivity studies conducted by NIST with the FDS model of the Howard Street tunnel evaluated variables in the fire scenario (e.g., tunnel geometry, fuel pool size, wall material properties), and determined that the heat release rate of the fire was limited to about 50 MW, due to oxygen starvation. Varying the fuel pool size can yield longer a duration fire, but peak fire temperatures are limited due to lack of sufficient oxygen in the confines of the tunnel. The 7-hr fire duration used to define the boundary conditions for the current study was obtained by assuming a fully ventilated fire that burned until all available fuel was consumed. The heat release rate for this fire scenario is approximately 500 MW, an order of magnitude higher than the heat rate predicted for a realistic representation of the fire conditions. Simulation of a longer fire requires reducing the burn rate or limiting the available oxygen for the fire, or both, which would result in lower fire temperatures. The scenario selected for the current study is a conservative representation of a potentially 'worst case' fire scenario for this accident. Additions to Chapter 2 expand the discussion of the fire scenario assumed in the FDS simulation used to determine the boundary conditions for the analyses of the SNF transportation packages.

No.	Comment	Response
5	In NRC's report on the Baltimore Tunnel fire, it appears that far too much emphasis is placed on investigating the possibility of loss of containment and not enough on the possibility of a loss of shielding scenario regarding the TN-68, Hi-Star 100, and NAC LWT SNF shipping casks. Loss of shielding is of particular concern to the Brotherhood of Locomotive Engineers and Trainmen for the following reasons:	Licensing regulations specified in 10 CFR 71 require that neutron and gamma shielding must be maintained within specified limits in all design basis accidents, including the regulatory fire transient. All three packages evaluated are expected to lose their neutron shield in the regulatory fire, and still maintain required neutron shielding. How this is accomplished is described in their respective SARs. Additions to Chapter 8 discuss the possible consequences of loss of neutron shielding and gamma shielding in terms of potential exposure. These analyses show that the potential dose would be below the limit of 1000 mrem/hr prescribed in 10CFR49 and 10CFR71 for all three packages in this fire scenario.
6	Shielding is an internal component of the cask design and any damage to the shielding would not be visually apparent to railroad employees.	All three packages evaluated can lose their neutron shield and still maintain external dose rates within regulatory limits, as documented in their respective SARs. Gamma shielding is provided by steel in the TN-68 and the HI-STAR 100 packages, and this shielding will not be reduced by any fire scenario. Some reduction of gamma shielding due to lead slumping as a consequence of melting and resolidifying is possible with the NAC LWT package. However, a significant increase in radiation dose from the NAC LWT would require physical damage to the package outer shell (such as a puncture), which could result in loss of lead shielding due to molten lead leaking from the package. Analysis of the conditions of this fire scenario show that the physical forces are not sufficient to result in damage to the package shell, and the lead shielding would remain within the cavity between the inner and outer shell during melting and resolidification. Potential dose increases due to possible slumping of the lead within the cavity are below the regulatory limit for accident conditions. Additions to Chapter 8 discuss the potential consequences of reduction in gamma shielding in the NAC LWT due to this fire scenario.
7	Train crews are not expected to be provided with dosimetry to measure off-link or on-link exposure during normal transportation, let alone emergency situations.	Additions to Chapter 8 discuss the potential consequences of loss of neutron shielding in all three packages, and potential reduction in gamma shielding in the NAC LWT due to this fire scenario. All three packages are designed to operate within regulatory limits without neutron shielding in place, and analysis shows

No.	Comment	Response
		that the NAC LWT also maintains radiation shielding within regulatory limits even when the potential reduction in gamma shielding is considered. Train crews that observe current regulations and procedures (e.g., 49 CFR part 171: §§ 171.15 and 171.16, 49 CFR part 172: subparts C G, and H, 49 CFR part 174: subparts A through D and K) governing the transportation of hazardous materials (including radioactive material) would not be at risk of exposure to hazards beyond the current regulatory limits for accident conditions from an SNF package subjected to the conditions of the Baltimore tunnel fire. It is the purpose of OCRWM and DOE to ensure that all appropriate measures are taken to protect carriers, workers, and the general public from adverse consequences associated with shipments of spent nuclear fuel and high-level radioactive waste. Regulations and procedures are currently in place that are designed to further the safety and security of SNF shipments. This includes instituting a "no pass" rule in tunnels for trains carrying radioactive material and trains carrying hazardous or flammable materials, to further reduce the extremely low probability of a tunnel fire accident involving an SNF transportation package (See discussion of AAR Circular OT-55 in Chapter 1.) This analysis of the Baltimore tunnel fire and previous evaluations (as discussed in Chapter 1) show that the risks associated with SNF shipments are extremely low. Additional measures under consideration to further mitigate the risk of this activity include - providing dosimeters for specific workers involved in the normal handling of SNF shipments - instituting 'dedicated' rail lines on specific sections of transportation routes where the consequences of an accident are deemed severe enough to warrant such precaution, despite the low probability of a severe accident

No.	Comment	Response
8	There are no plans to equip locomotives with radiation detectors to alert crews to dangerous spikes in dose rate.	See response to Comment 7 above.
9	In all three models, the loss of neutron shielding was a given, but loss of gamma shielding was scarcely touched upon. Lead has a melting point of 621 degrees[F (328°C)]. In all three models, the gamma shield exceeded that temperature. The TN-68 exceeded that temperature after 5 hours, both the Hi-Star 100 and the NAC LWT casks reached that point in just two hours. The NAC LWT uses lead rather than carbon steel as its gamma shield. The shielding would have likely failed at the two-hour mark, eventually reaching 1378 degrees[F (748°C)] after 6.75 hours in the fire.	Gamma shielding is not lost in the TN-68 or HI-STAR 100 during the Baltimore tunnel fire, since these packages use steel for gamma shielding, not lead. For the NAC LWT, the lead reaches its melting point, but in this accident scenario, the lead remains encapsulated within the steel shell of the package body and base, and continues to function as a gamma shield. Additions to Chapter 8 provide an expanded discussion of the consequences of the lead melting during the fire, and the consequent effect on gamma shielding in the NAC LWT. The analyses presented show that this package maintains shielding such that the dose rate at 1 meter from the package surface is below 1000 mrem/hr, as required in all accident conditions. (See response to Comments 5, 6, and 7.)
10	The final version of NUREG/CR-6886 should include an expanded introductory section summarizing previous NRC studies of spent fuel shipping cask response to severe fire environments, including an explanation of the relationship between this report and NUREG/CR-6672 (SAND2000-0234).	There is no direct relationship between NUREG/CR-6672 and NUREG/CR-6886. NUREG/CR-6672 undertakes a detailed study of the risks associated with the transport of spent nuclear fuel by all possible modes, considering both mechanical loads and thermal loads imposed by conservatively defined bounding accident scenarios. Thermal loads were evaluated by postulating an extremely long duration (11 hours) fully engulfing pool fire at 1832°F (1000°C), which readily envelopes the "worst case" possibilities presented by any historical fire accident, including the Baltimore tunnel fire. The analyses in NUREG/CR-6672 use extremely conservative assumptions and highly simplified models of SNF packages for the thermal analyses, which tend to severely over-estimate the peak temperatures within the package, and do not consider the three-dimensional effects of a tunnel fire or any specific historic accident scenarios. The main effect of the modeling simplifications and conservatisms in

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		NUREG/CR-6672 is to grossly over-estimate the peak predicted temperatures in an SNF package in the response to any fire scenario. Even with extremely conservative bounding assumptions, including assumptions related to accident frequency, severity and consequences, the analysis in NUREG/CR-6672 shows that the risks associated with the shipment of spent nuclear fuel by truck or rail are very small. The report further concludes that current regulations governing the transportation of spent nuclear fuel "adequately protect public health and safety."
11	The final version of NUREG/CR-6886 should include a more detailed discussion of the Nation[al] Transportation Safety Board (NTSB) investigation of the Baltimore Tunnel Fire, including the NTSB safety recommendations (R-04-15 and -16, issued January 5, 2005) and the NTSB decision not to issue an official report on the cause and history of the fire.	As discussed in Chapters 1, 2, 3, 4, 5 and 6 of NUREG/CR-6886, information from the NTSB was used in the process of determining a conservative representation of the Baltimore tunnel fire scenario, as well as consultations with experts at NIST and CNWRA. The NTSB performed a thorough investigation of this accident, but declined to issue a final report because the Board could not come to a decision on the cause of the accident. The cause of the accident is not relevant to the analyses presented in NUREG/CR-6886, which accepts as a given that the accident did indeed occur. Similarly, the NTSB safety recommendations R-04-15 and -16 are not relevant to the fire analysis. These recommendations concern the need for improved communications between CSX and the city of Baltimore, and improvements to the city's emergency preparedness plans.
12	The final version of NUREG/CR-6886 should include a detailed discussion of the 2001 analysis of the Baltimore Tunnel Fire prepared by Radioactive Waste Management Associates for the State of Nevada.	NUREG/CR-6886 is a case study of a historical event, not a peer review of other work related to general transportation accidents involving radioactive materials. The RWMA study is particularly problematic, since it is based on significantly different assumptions regarding the fire and the properties of the SNF packages, such that it is impossible to make meaningful comparisons between the two reports. The RWMA study was released less than 3 months after the accident, long before the NTSB, CNWRA, NIST, and NRC had finished investigating the event, and as a result the RWMA study is based on inaccurate and unsubstantiated assumptions about the nature, duration, and intensity of this fire scenario. The RWMA report overstates the intensity and duration of the fire (assuming a 5-day

No.	Comment	Response
		fire duration for the intense portion of the fire vs. the 3-hour duration confirmed by NTSB investigations.) The RWMA study inappropriately uses temperature predictions from the long-duration pool fires analyzed in NUREG/CR-6672 to estimate the tunnel fire environment. The RWMA report incorrectly models the behavior of the package and spent fuel, assuming an incorrect failure mechanism for fuel cladding (i.e., creep vs. pressure rupture), and neglects credible resistances in the release pathway (e.g., metal to metal contact and lid torque.) The RWMA report also overestimates the amount of Cesium that is available for release from the fuel rods. As a result, the RWMA report vastly overestimates the potential consequences of the Baltimore tunnel fire scenario when applied to an SNF package, and does not present a reliable analysis that could assist in determining the risks associated with transportation of spent nuclear fuel by rail.
13	The final version of NUREG/CR-6886 should include a detailed discussion of the 2002 analysis of the Baltimore Tunnel Fire prepared by the U.S. Department of Energy as part of the Final Environmental Impact Statement for Yucca Mountain (DOE/EIS-0250).	A direct comparison between the analyses in NUREG/CR-6886 and in DOE/EIS-0250 is not meaningful. The analysis in EIS-0250 does not evaluate the Baltimore tunnel fire specifically; instead it considers the maximum reasonable foreseeable accident, which is considered to envelope events such as the Baltimore tunnel fire scenario.
14	The final version of NUREG/CR-6886 should include side-by-side fire transient results and consequence analyses of the NAC LWT cask, with and without enclosure in an ISO container. (The discussion at page 7.17 implies that these analyses were performed, but they apparently were not reported.)	The NAC LWT was not analyzed without an ISO container in this study. This package was analyzed enclosed in an ISO container because that is the anticipated mode of transport when it is shipped by rail. The CoC for the NAC LWT requires that it be enclosed in either a personnel barrier (PB) or an ISO container. PBs commonly used for trucks are not shippable by rail, so for rail transport, an ISO would generally be required. Current DOE policy requires an ISO for truck packages shipped by rail, and every rail shipment of the LWT to date has been in an ISO container. The discussion on p. 7.17 is intended to show that the ISO container does not substantially shield the NAC LWT package from the fire, and peak component temperatures would be essentially the same, with or without an ISO container.

No.	Comment	Response
15	The final version of NUREG/CR-6886 should include an additional cask analysis, parallel to the approach described in Section 5, of a General Atomics GA-4 legal-weight truck cask, shipped on a rail car without enclosure in an ISO container.	This study evaluated the performance of three representative packages currently in service, based on resources that are postulated to be used. Including analyses for the GA-4 package in NUREG/CR-6886 would not be expected to substantially alter the results or conclusions obtained in this study. In addition, the thermal performance of the GA-4 package in an extra-regulatory fire has already been examined in NUREG-1768, *United States Nuclear Regulatory Commission Package Performance Study Test Protocols*. Additional analyses may be warranted for future studies, if the staff believes large scale use of a particular package is expected.
16	The final version of NUREG/CR-6886 should include an additional thermal analysis for each of the four casks, parallel to the approach described in Section 5, assuming that the cask is located 5 meters (16 feet) from the fire center.	As noted in the response to Comments 1, 2, and 3, the selected location of the SNF packages for this analysis is consistent with the physical attributes of the tunnel and the possible shipping configurations for an SNF package in the Baltimore tunnel fire scenario.
17	The final version of NUREG/CR-6886 should include an additional thermal analysis for each of the four casks, parallel to the approach described in Section 5, assuming that the cask is located within the hottest region of the fire.	See response to Comments 1, 2, 3, and 16.
18	The final version of NUREG/CR-6886 should include a re-examination of the potential for fuel cladding failure and release of radioactive materials, including fission products, at temperatures below the projected burst temperature of 1382°F (750°C) for Zircaloy cladding. (Additional attention should be given to the presence of older fuel with brittle and/or previously failed cladding.)	The limit of 1382°F (750°C) is a conservative lower bound on the temperature at which Zircaloy cladding might be expected to fail by burst rupture. There is no reason to suppose that this limit is not sufficient for fuel within the TN-68 cask when exposed to the Baltimore tunnel fire scenario, since this cask is licensed to carry only intact fuel assemblies. The HI-STAR 100 is licensed to carry failed fuel, but this analysis shows that this cask would not be expected to lose containment in the Baltimore tunnel fire scenario. This package design employs an inner canister (MPC) that is conservatively predicted to maintain its integrity throughout the entire fire transient. Radioactive materials, including fission

No.	Comment	Response
		products, would not be released from this package, even under conditions as severe as the Baltimore tunnel fire scenario. The NAC LWT is also licensed to carry failed fuel, but this package is quite small and can carry only a limited amount of spent nuclear material, its largest payload consisting of a single PWR assembly. Analysis of the consequences of postulating 100% failure of all rods in a single PWR assembly consisting of high burn-up, 3-yr-cooled fuel (see NUREG/CR-6672) shows that the potential release from this package remains below an A_2 quantity for this fire scenario, as discussed in Chapter 8.2.5. The available fission products from one PWR assembly of this type far exceeds that of any failed fuel the NAC LWT is licensed to carry. A payload that includes failed fuel does not adversely affect the potential consequences of the Baltimore tunnel fire scenario. Additional discussion of the potential consequences of the Baltimore tunnel fire scenario for the HI-STAR 100 and the NAC LWT when carrying failed fuel has been added to Chapter 8.
19	The final version of NUREG/CR-6886 should include a reexamination of the potential for fuel cladding failure and release of radioactive materials for higher burn-up fuels, specifically addressing the issues of radiation embrittlement, pellet degradation due to thermal cycling, and fission product buildup.	This analysis was performed assuming that all of the packages would be loaded with design basis fuel, based on the cask's licensing qualifications. The TN-68 and HI-STAR 100 packages are not licensed to carry high burn-up fuel. The NAC LWT is the only package considered in this study that is licensed to carry high burn-up fuel, in which case the total fuel load is limited to no more than 25 rods. As noted in the response to Comment 18, an analysis assuming 100% failure of all rods in a single high burn-up, 3-yr-cooled PWR assembly shows that the potential release from this package remains below an A_2 quantity for this scenario. The available fission products from one PWR assembly of this type far exceeds that of the maximum of 25 high burn-up fuel rods the NAC LWT is licensed to carry.

No.	Comment	Response
20	The final version of NUREG/CR-6886 should include a reexamination of the potential for release of radioactive materials for fuel assemblies with higher levels of CRUD activity (e.g., BWR assemblies with surface concentration up to150 μCi/cm^2).	The current analysis (see Chapter 8) was performed assuming maximum CRUD activity of 300 μCi/cm^2, and corresponding average activity of 150 μCi/cm^2 for the TN-68. Given the conservative assumptions on the amount of CRUD that can detach from the rod surfaces and plate out, and the fact that 90% of the rods are cleaner than this assumed level of activity, this assumption is appropriately conservative for this analysis.
21	The final version of NUREG/CR-6886 should include a reexamination of the mechanisms for seal failure and release of radioactive materials, including seal failure long before maximum seal temperatures are reached, bolt failure, and pressure-induced blowout of failed seals.	Failure due to exceeding temperature limits is the only credible cause of seal failure in this accident scenario. The specified limits are inherently conservative, in that they are based on long-term service temperature limits, rather than transient limits. Temperatures are not high enough to consider bolt failure possible, and internal pressure increase is not sufficient by itself to compromise seals. As discussed in Chapter 8, the potential release of radioactive materials is not limited by the condition of the seals or by the time required for the seals to fail. The conclusion that there would be no release from the HI-STAR 100 is based on the welded inner canister remaining intact, not simply the integrity of the seals. For the TN-68 and the NAC LWT, the seals are assumed to fail, and the amount of the potential release is based primarily on the amount of CRUD material available for release from the package. It is not dependent on the time or mode of seal failure. The potential release is determined using a model developed by Sandia National Laboratory for analysis of CRUD contribution to shipping package containment requirements (SAND88-1358; see Ref. 26).
22	The final version of NUREG/CR-6886 should include a reexamination of the role of the HI-STAR 100 train carriage and cask restraints regarding heat shielding and heat conduction.	Heat shielding effects of these structures during the fire would act to decrease the heat load on the package during the fire; heat conduction after the fire would serve to hasten cool-down. Assumptions made in the analysis are conservative for both the fire and post-fire cool down.

No.	Comment	Response
23	The final version of NUREG/CR-6886 should include a discussion of the emergency response implications, and cask recovery implications, of the predicted damage to the neutron shielding for all three considered casks, and the loss of gamma shielding for the NAC LWT.	The loss of neutron shielding is expected in all 3 designs as a consequence of the regulatory fire (i.e., 30 minutes at 800°C). Existing regulations and procedures regarding emergency response should be sufficient for this scenario, as well. The NAC LWT does not lose its gamma shielding in this scenario. The lead melts during the fire, but is confined and held in place by the steel package body. Additional discussion has been added to Section 8.1 evaluating the consequences of lead melting and resolidification in this package. (See responses to Comments 5 through 9 above.)
24	The final version of NUREG/CR-6886 should include a reexamination of the uncertainties associated with the NIST FDS simulations of gas and wall temperatures 20-30 meters from the fire center. (These issues include the construction and benchmarking of the FDS code, selection of the conductivity value for the tunnel bricks, and potential inconsistencies with the materials analyses.)	Because of uncertainties and unknowns related to the fire scenario, the FDS simulation and the package analyses were performed using conservative assumptions. The results of the FDS simulations using realistic assumptions are in close agreement with the peak temperatures estimated from analyses of material recovered from the tunnel after the fire. (See the discussion in Chapter 3.) In addition, sensitivity studies were performed with FDS to determine the effect of varying parameters that could potentially affect peak predicted temperatures, including the thermal conductivity of the tunnel wall surfaces. The analysis predicting a fire duration of 7 hours is the result of specifying parameters that assume an unrealistically high rate of oxygen flow to the fire, in order to achieve complete combustion of the entire inventory of available fuel. The resulting fire conditions are an order of magnitude hotter than conditions predicted using realistic assumptions for the fire scenario. Variation in parameters due to uncertainties would generally result in a less severe fire transient. Additions to Chapters 2, 4, 5 and 6 expand the discussion of the conservatisms in the FDS analysis of the fire scenario and the modeling approach used in the analyses of the SNF packages.

No.	Comment	Response
25	The final version of NUREG/CR-6886 should include a comprehensive analysis of uncertainties in the following factors, and how these uncertainties might affect the results of the consequence assessment: fire size, location, and duration; gas and wall temperatures from the NIST FDS simulations; CNWRA metallurgical analyses; uncertainties in the package models; seal and cladding temperature limits; and heat transfer models for the neutron shield (including gap radiation in charred solid, and boiling heat transfer in liquid) and impact limiters.	Relevant discussions of all of these issues are included in the publicly posted version of the report, and have been expanded in Chapters 2, 5, 6, and 8 of the current Revision 1. Uncertainties related to all of these enumerated issues were considered and accounted for in a conservative manner in these analyses. Evaluation of less conservative variations within the range of uncertainties in these factors would result in shorter fire durations and lower fire temperatures, which would lower predicted package component temperatures.
26	The final version of NUREG/CR-6886 should include a discussion of any peer reviews conducted for this report, and any peer reviews conducted for two of the major supporting studies, NUREG/CR-6793 (NIST) and NUREG/CR-6799 (CNWRA).	NUREG/CR-6886 has not been subjected to external peer reviews. Instead, this document has undergone intense internal technical peer reviews by PNNL and NRC before publication, and was made available for public comment for a period of approximately 3 months. This permitted independent review by any and all interested parties. All public comments on this document are included in the final publication. An external peer review was not deemed necessary because of the very low risks associated with this scenario. This is due to the low frequency of the type of accident and the minimal consequences of postulated accident conditions. The observed frequency is once during 21 billion miles of train travel, which comprises the last 30 years of historical rail shipments. The potential consequences are estimated to be less than 0.3 of an A_2 quantity of release, and the analysis predicts large margins of temperature against cladding failure. For this study, a peer review would not be cost effective.

No.	Comment	Response
27	The possibility of fuel oil fire temperatures of 1650-2000°C for periods of time far in excess of the 30-minute test characteristic of Type B casks, make it impossible to consider that the circumstances know[n] about the Baltimore tunnel fire would be the worst circumstances that would be likely to apply in a fire situation affecting nuclear waste casks, during their transport.	The analyses in NUREG/CR-6886 predict the effects that a particular historical fire accident could be expected to have on three specific SNF transportation packages. This report does not attempt to define the worst possible fire accident. However, this is an extremely severe accident with a statistical frequency on the order of one such accident in 21 billion miles of train travel. This accident is bounded by the analyses in NUREG/CR-6672 and NUREG-0170 evaluating the risks associated with transportation of spent nuclear fuel.
28	The Advisory Committee on Nuclear Waste (ACNW) inquired during a public meeting on September 21, 2006, as to whether or not the figure of 21 billion rail miles traveled between 1975 and 2005, cited in the report, included DOE Naval Nuclear Propulsion Program waste shipments.	This mileage figure includes all commercial rail transportation for this period of time; however, it was not broken down into specific categories of rail transportation. DOE Naval Nuclear Propulsion Program waste shipments are commonly done on commercial railways and, as a result, would be included in this number.

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10. SUPPLEMENTARY NOTES

C. Bajwa, NRC Project Manager

11. ABSTRACT *(200 words or less)*

On July 18, 2001, a freight train carrying hazardous (non-nuclear) materials derailed and caught fire while passing through the Howard Street railroad tunnel in downtown Baltimore, Maryland. The United States Nuclear Regulatory Commission (USNRC), one of the agencies responsible for ensuring the safe transportation of radioactive materials in the United States, undertook an investigation of the train derailment and fire to determine the possible regulatory implications of this particular event for the transportation of spent nuclear fuel by railroad.

The USNRC assembled a team of experts from the National Transportation Safety Board, the National Institute of Standards and Technology, the Center for Nuclear Waste Regulatory Analyses, and the Pacific Northwest National Laboratory to determine the thermal conditions that existed in the Howard Street tunnel fire and analyze the potential effects of those conditions on various spent nuclear fuel (SNF) transportation package designs.

The staff's evaluation indicates that neither SNF particles nor fission products would be released from a package, carrying intact SNF, involved in a severe tunnel fire such as the Baltimore tunnel fire. A release of CRUD from the surface of fuel cladding, while a possibility for certain package designs, is highly unlikely, and would be within regulatory limits.

12. KEY WORDS/DESCRIPTORS *(List words or phrases that will assist researchers in locating the report.)*

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Approved by

Lucien M. Papouchado, Program Manager,
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Many persons in the Savannah River Laboratory and Plant are responsible for the data in this report. In particular, the contributions of John R. Fowler and Richard E. Edwards for material and curie balances, and M. John Plodinec for glass chemistry and leachability, were important to the preparation of this document.

ABSTRACT

This document describes the reference wasteform and canister for the Defense Waste Processing Facility (DWPF) and updates DP-1606, Rev. 1, which was published in August 1983. The principal changes include revised feed and glass product compositions, an estimate of glass product characteristics as a function of time after the start of vitrification, and additional data on glass leaching performance. The feed and glass product composition data are identical to that described in the DWPF Basic Data Report, Revision 90/91 (see references).

The DWPF facility is located at the Savannah River Plant in Aiken, SC, and it is scheduled for construction completion during December 1989.

The wasteform is borosilicate glass containing approximately 28 wt % sludge oxides, with the balance consisting of glass-forming chemicals, primarily glass frit. Borosilicate glass was chosen because of its stability toward reaction with potential repository groundwaters, its relatively high ability to incorporate nuclides found in the sludge into the solid matrix, and its reasonably low melting temperature. The glass frit contains approximately 71% SiO_2, 12% B_2O_3, and 10% Na_2O.

Tests to quantify the stability of DWPF waste glass have been performed under a wide variety of conditions, including simulations of potential repository environments. Based on these tests, DWPF waste glass should easily meet repository criteria.

The canister is filled with about 3,700 lb of glass which occupies 85% of the free canister volume. The filled canister will generate approximately 690 watts when filled with oxides from 5-year-old sludge and precipitate from 15-year-old supernate. The radionuclide activity of the canister is about 233,000 curies, with an estimated radiation level of 5,600 rad/hour at the canister surface.

The canister is fabricated of standard 24 in. OD, schedule 20, type 304L stainless steel pipe with a dished bottom, domed head, and a combined lifting and welding flange on the head nozzle. The overall canister length is 9 ft. 10 in. (300 cm) with a wall thickness of 3/8 in. The canister length was selected to establish a practical cell height in the DWPF. The canister diameter was selected as an optimum size from glass quality considerations, a logical size for repository handling, and to ensure that a filled canister in either a single- or a double-containment shipping cask could be accommodated on a legal-weight truck. The overall dimensions and weight are compatible with repository requirements.

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DEFENSE WASTE PROCESSING FACILITY WASTEFORM AND CANISTER DESCRIPTION

INTRODUCTION

This document describes the reference glass wasteform and canister for the Defense Waste Processing Facility (DWPF). The borosilicate glass wasteform and stainless steel canister are the reference package selected in December 1982, and they are the basis for the DWPF design and process.

HIGH LEVEL WASTEFORM CHARACTERISTICS

The wasteform for the DWPF is a borosilicate glass containing approximately 28% sludge oxides with the balance consisting of glass-forming chemicals, primarily glass frit. Borosilicate glass was chosen as a wasteform because of its stability toward attack by water, its relatively high ability to dissolve nuclides found in the sludge into the melt, its relatively low melting temperature, and because the process is based on well-developed technology.

Description of the waste glass characteristics is divided into three sections: composition, mechanical properties, and chemical stability. Sludge-precipitate processing is based on processing the 5-year-old or older sludge plus a 15-year-old or older supernate fraction. The sludge fraction contains the strontium and plutonium, and the supernate portion contains virtually all of the cesium. Mechanical properties of the waste glass are based on a typical frit, designated as Frit 131.* Other frits, such as Frits 165 and 200, have similar mechanical properties, based on experimental laboratory tests.

Data on the stability of waste glasses described in this report are from glasses simulating the DWPF product, which are based on Frit 165. Although the glasses produced in the DWPF will not be identical to glasses made from Frit 165, due to changes in the DWPF process after the development of Frit 165, the chemical stability is expected to be similar, based on experimental laboratory tests (see Glass Stability Programs under References).

COMPOSITION OF DWPF WASTE GLASS

Feed to the DWPF consists of two streams: settled, washed sludge and supernatant liquid. The sludge consists of hydroxides and hydrous oxides containing nearly all of the stable and radioactive fission products, and actinide elements, as well as elements added in the SRP separations processes. These are primarily iron, manganese, aluminum, and mercury. The sludge is treated with sodium hydroxide to dissolve hydrated aluminum oxides, washed with water to remove soluble salts to 2% on a dry basis, and then allowed to settle. The washed sludge is transferred as a 13% slurry to the DWPF slurry receipt adjustment tank (SRAT).

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*Savannah River Laboratory frit designations.

The supernate solution containing dissolved salts is treated in the waste tanks with sodium tetraphenylborate to precipitate cesium, and with sodium titanate to absorb the trace amounts of strontium and plutonium compounds present in the supernate. These precipitates are transferred to the DWPF salt processing cell where the organic portion of the salts are decomposed to benzene and the inorganic portion is transferred to the slurry receipt adjustment tank.

Sludge Processing

A description of the chemical composition of sludge feed is shown in Tables 1A and 1B, the radionuclide content in Table 2, and the isotopic content in Table 3. The soluble solids (Table 1A) are principally $NaNO_3$ (41%), $NaNO_2$ (19%), NaOH (18%), and $NaAl(OH)_4$ (11%), which constitute about 89% of the soluble solids. Of the insolubles (Table 1B; zeolite composition is given in Table 18), $Fe(OH)_3$ (41%), $Al(OH)_3$ (16%), MnO_2 (7%), and $UO_2(OH)_2$ (7%) constitute approximately 70% of the insoluble solids. Activity of the sludge feed is 133 Ci/gal (Table 2) with a decay heat of 0.44 watt/gal for 5-year-old waste. Of this activity, 78% is due to Sr-90, Y-90, and Pm-147.

Precipitate Processing

A description of the chemical composition of the precipitate feed to the salt cell is shown in Table 4, the radionuclide content of feed from the precipitate process is shown in Table 5, and the isotopic content in Table 6. The principal compounds of the precipitate feed, on a water-free basis, are potassium tetraphenylborate (KTPB) (76%), $NaNO_3$ (6%), and NH_4TBP (4%); these compounds constitute about 85% of the input stream. Activity of the feed from the precipitate process is 71.4 Ci/gal with a decay heat of 0.167 watt/gal for precipitate from 15-year-old supernate. Of this activity, about 99% is due to Cs-137 and its beta decay daughter Ba-137m.

The chemical composition of the feed from the salt cell to the SRAT is shown in Table 7, the radionuclide content in Table 8, and the isotopic content in Table 9. The principal components, on a water-free basis, are KCOOH (29%), H_3BO_3 (19%), and NaCOOH (13%). Activity of this feed to the SRAT is 76.5 Ci/gal with a decay heat of 0.178 watt/gal.

The chemical composition of combined sludge and precipitate waste glass is shown in Table 10, the radionuclide content is shown in Table 11, and the isotopic content in Table 12. Total activity is 63.1 Ci/lb with a decay heat of 0.187 watt/lb for 5-year-old sludge and precipitate from 15-year-old supernate. Thus, the 3,700 lb of glass in a DWPF canister contains about 233,000 Ci with a decay heat of 690 watts. The isotopes Y-90, Sr-90, Cs-137, Ba-137m, and Pm-147 contribute about 87% of the activity.

Chemical composition of the design basis frit, designated as Frit 200, is shown in Table 13A. Composition of the frit to be used during initial operations, Frit 202, is also shown. Frit 202 is approximately 77% SiO_2, 8% B_2O_3, and 7% Li_2O. The frit was developed after an extensive series of tests designed to produce a waste glass product with good leach resistance, high solubility for waste oxides, and a practical melting temperature. It is based on earlier efforts which resulted in the development of Frit 165. The performance of the DWPF glass is expected to be similar to that of Frit 165 glasses, based on experimental laboratory tests. Compositions of glasses expected to be produced by the DWPF are shown in Table 13B.

PHYSICAL PROPERTIES OF DWPF WASTE GLASS

Physical properties of DWPF waste glass have been measured and estimated by calculation. Most of the properties determined by experiment are based upon Frit 165 rather than the Frit 200; however, there are few significant differences. The principal differences between the two are that Frit 200 is higher in percent of SiO_2 and B_2O_3, but contains no ZrO_2. A chemical comparison between several of the frits evaluated is shown in Table 13A.

Physical properties of glass wasteforms are listed in Table 14. Of these values, the fractional thermal expansion, the density at 100°C, and the softening point were experimentally determined for Frit 165 glass. Other values are based on Frit 21 or other typical compositions.

Several other physical properties of SRP waste glasses have been estimated by calculation. Heat capacity, thermal conductivity, and density for three types of DWPF waste glass (composite, high iron, and high aluminum) have been calculated on the basis of glass containing approximately 28% sludge oxides and the balance glass Frit 131. Physical properties of waste glasses made in the range of frit compositions expected in the DWPF showed that these properties were invariant with changes in frit composition. Typical compositions of waste for these three types of glass are shown in Table 15.

Heat Capacity

Measured and calculated heat capacities of simulated waste glasses are listed in Table 16. C_{pt} is the true heat capacity at the indicated temperature. True specific heat as a function of temperature is also shown in Figure 1.

Density

Measured densities for simulated waste glass are listed in Table 17, and density as a function of temperature is shown in Figure 2 for the range of glasses expected in DWPF.

Thermal Conductivity

Calculated thermal conductivity of DWPF glass as a function of temperature is shown in Figure 3.

Electrical Resistivity

Measured electrical resistivity of the glass melt as a function of temperature is shown in Figure 4. At the operating melt temperature of 1,150°C, the resistivity is approximately 2.5 ohm-cm for composite glass.

Thermal Expansion

Waste glass measured thermal expansion as a function of temperature is shown in Figure 5 for composite glass, in Figure 6 for high-Al glass, and in Figure 7 for high-Fe glass.

Viscosity

Experimentally determined viscosities for the range of glasses expected in the DWPF are shown in Figure 8. At the nominal operating temperature of 1,150°C, the composite glass viscosity is about 30 poise.

CHEMICAL STABILITY OF DWPF WASTE GLASS

In accordance with the Nuclear Waste Policy Act of 1982, the canisters of waste glass produced in the DWPF will eventually go to a licensed federal repository for permanent disposal. Recent legislation indicates that the first repository will be in tuff at the Nevada Test Site. At the repository, the canister containing waste glass will be emplaced in the geology as part of a waste package. This package will contain the waste glass, the type 304L stainless steel canister, a metallic overpack to meet the containment requirement of 10 CFR 60, and possibly a packing material such as crushed rock or clay.

Reaction of waste glass with repository groundwater is the most likely mode of release of long-lived radioactive species to the environment. Borosilicate glass was chosen as the wasteform for the DWPF because of its stability when exposed to groundwater. Thus, Savannah River Laboratory (SRL) has focused on developing a quantitative understanding of the reaction between glass and groundwater over the range of conditions expected for liquid groundwater and DWPF glass interactions in candidate repository environments.

The expected range of conditions for each of three geologies is shown in the following table. The ranges have been derived from reference repository conditions for each of the geologies. The values are based on the assumption that the waste package containing DWPF glass in the repository will meet regulatory requirements, particularly the containment requirement imposed by the Nuclear Regulatory Commission in 10 CFR 60.

EXPECTED CONDITIONS FOR INTERACTIONS BETWEEN GROUNDWATER AND DWPF GLASS

PARAMETER	REPOSITORY CONDITION		
	SALT	BASALT	TUFF
Temperature	34 - 90°C	57 - 150°C	30 - 95°C
Pressure	2800 PSI	4700 PSI	Atmosperic
Groundwater	Brine	Silicate	Dilute Silicate
Eh	≈ 0 v	- 0.40 v	Oxidizing
pH	6	9.75	7.5
Flow	Static	Very Slow	Intermittent
Amount	Limited	Flooded	Limited

Studies on glass stability have been in progress at the Savannah River Laboratory for the past ten years. Early glass leaching characteristics of SRP simulated and actual waste glasses are summarized in report DP-1629 (see reference section). These early studies showed that DWPF glass reacted very slowly with groundwaters, and could immobilize the radionuclides in SRP waste. In this section, more recent results are summarized.

The program being carried out by SRL has two major components: mechanistic studies, and verification. SRL's mechanistic studies are directed toward developing a quantifiable model of long-term release from DWPF waste glass, while verification studies test the validity of the model and its predictions. Although these are separate functions, there is necessarily a large amount of interaction between the two components. For example, leaching models are used in the design of verification experiments to point out the appropriate parameters to measure. Conversely, verification tests can indicate phenomena not considered in the modeling program, and thus are used to guide modeling efforts.

Glass - Groundwater Reaction Mechanisms

The SRL programs to identify and quantify the mechanisms of the reactions between waste glass and repository groundwaters include:

- Fundamental studies designed to quantify the effects of parameters such as glass composition, groundwater composition (including Eh, pH, and dissolved gases), or radiation on glass durability.
- Laboratory tests designed to quantify glass performance under conditions simulating actual potential repository environments.

The first item includes both theoretical and experimental efforts. The thermodynamic approach, first suggested by Paul and Newton, has been an important tool which has enabled SRL to compare the performance of a wide range of glasses and minerals based on their compositions. As Figure 9 shows, the performance of basalt from the Hanford reservation is virtually indistinguishable from that of the DWPF product.

SRL is also performing repository simulation tests in the laboratory, using both actual and simulated waste glasses. These tests are providing data which will be used to determine the stability of DWPF glass in a given repository. In these tests, waste glass and stainless steel samples (simulating a breached canister) are placed in a reaction vessel made from rock representative of one of the candidates for a repository - tuff, basalt, or salt (Figure 10). The reaction vessels used in these experiments are rock cups made from either tuff from outcrops at the Nevada Test Site, basalt from outcrops on the Hanford site, or salt from the WIPP site in New Mexico. Groundwater is then placed in these rock cups, and the cups are closed. For the tests in tuff, actual groundwater from a well (J-13) at the Nevada Test Site is used. In the case of basalt, a synthetic groundwater (GR-4), prepared in an oxygen-free environment, is used. For the salt tests, both inclusion and intrusion brines are used.

Although these laboratory tests are not all completed, they all indicate that the amount of radioactivity which will be free to travel with the groundwater will be a small fraction of the activity present in the waste glass. The results that follow are from the radioactive tests in tuff cups, in terms of concentrations.

In these tuff tests, solution concentrations of most elements were constant within experimental error after approximately 40 days, indicating that the rate of alteration of the glass had become very small. The final concentrations of species in solution were then used to provide estimates for the amount of material released by the waste glass. The concentrations were multiplied by an extremely conservative upper bound for the amount of groundwater which would be available for reaction (50 L), and then divided by the inventory of the individual species. This yielded the estimates of fractional release from these waste packages. The small fractions released are 500 - 1000 times less than the NRC requirement for the waste package as a whole. Similar numbers result from application of mass transfer methodology to the results of these tests. Thus, the tests indicate that DWPF glass should perform well in this environment.

RESULTS OF TUFF SIMULATED REPOSITORY TESTS

SPECIES	FINAL CONCENTRATION	ANNUAL FRACTIONAL RELEASE
Cs	0.9 µg/mL	6×10^{-8}
Sr	2.0	3×10^{-8}
Pu	0.03	1×10^{-8}

Verification Testing

The results and conclusions from SRL's mechanistic efforts are being verified in several ways:

- Extensive testing of waste glass in burial experiments in underground laboratories, to relate performance in the laboratory to the actual repository.
- Large-scale leaching experiments using thick slices from full-scale canisters of simulated waste glass, to relate the performance of laboratory-size samples to that of full-scale canisters of waste glass.
- Extensive testing of simulated and actual waste glasses prepared according to the DWPF process, to relate the performance of laboratory-prepared samples to that of glass made in the DWPF.

The most advanced of these verification programs is that in which samples of simulated waste glass have been buried in underground facilities. Extensive testing has been carried out in the Stripa mine in Sweden, where samples of several simulated waste glasses have been buried in granite for over a year. In this joint effort, scientists from SRL, KBS (the Swedish nuclear program), and the University of Florida have found only a slight interaction between glass and groundwater in the first month of testing, and virtually none thereafter. This agrees well with laboratory tests which also show that steady-state is reached rather quickly. The thermodynamic approach previously alluded to was also applied to these tests, with the results shown in Figure 11. The amount of material released in two years was approximately 50 times less than regulatory limits.

A more extensive set of burial tests has begun in the Waste Isolation Pilot Plant (WIPP) facility in New Mexico. In these tests, samples of simulated waste glasses from seven countries have been emplaced in salt approximately 2,000 feet underground. These samples are being subjected to brine attack under both expected and unexpected but possible conditions in a salt repository.

The relevance of the mechanistic studies have also been verified in other ways. For example, full-scale canisters of nonradioactive simulated waste glass, filled according to the DWPF process in the SRL Engineering Test Facility, have been sliced into sections 18 to 24 inches high. These large slices were then immersed in large leach vessels of deionized water, and leached under conditions approximating the standard MCC-1 test. A companion set of experiments was performed with laboratory-size samples of the same glass to determine the appropriate relationship between laboratory and full-scale tests. When differences in surface preparation of the samples were removed from the data, there was excellent agreement between the two data sets.

SRL is also continuing to rigorously characterize and test glass samples made according to the DWPF process. The purpose of this effort is to establish that the results of tests of laboratory-prepared samples are relevant to the performance of DWPF glass. For example, samples of glass of the same composition were prepared in a 50 cc crucible, a 3 kg continuous electric melter, and in a 1,500 kg capacity continuous melter, with residence times ranging from 3 to 70 hours. As shown in Figure 12, the performance of the glass did not vary appreciably with the size of the melter. Thus, the performance of DWPF glass actually produced in the DWPF will be similar to that of glass made in the laboratory. Similar studies will determine the effects of other processing variables, such as melt temperature, on the performance of the DWPF product. Ultimately, a response surface model relating process variables to product performance will be generated.

DWPF CANISTER

Canister Grapple Assembly

The lifting grapple is specific for the DWPF canister and was developed by Remote Technology Corp. (REMOTEC) of Oak Ridge, TN (see references). The design is described in detail in the literature, therefore only the principles will be described here. Figure 13 is an assembly drawing..

Maximum size	Diameter =	600 mm
	Length =	1,000 mm
Capacity	6,820 kg, rated.	
Operation	Two-step release, failsafe. Transported by in-cell crane.	
Mechanism	All mechanical.	
Design life	60,000 cycles over 5 years without lubrication.	
Repair	Contact maintenance after high pressure wet decontamination.	
Failure recovery	Manual release activated by 4 kg maximum pull force.	
Materials	Stainless steel.	
Testing	Load test:	125% of rated load
	Cycle test:	500 cycles at rated load
	Misalignment:	Engage canister neck with 25 mm offset from grapple centerline
	Collision:	Strike object with crane traveling at 9 m/min.

Canister Dimensions

Canister dimensions and weight are shown in Figure 14 from drawing W832094 - Rev. 5, "DWPF Canister Assembly".

Principal dimensions and tolerances are:

Overall length	118.00 in. ± 0.06 in.
Outside diameter	24.00 in. ± 0.12 in.
Wall thickness	3/8 in. nominal pipe tolerance
Bow	0.12 in. max
Surface finish	125 rms
Inside volume	26.0 ft^3 nominal
Weight, empty	1100 lb
Weight, 85% full	4800 lb
Material	Type 304L stainless steel

Material of Construction

Type 304L stainless steel was chosen as the canister material for vitrified waste using the continuous melter process. This recommendation is based on long-term heating tests for up to 20,000 hours (2.3 yr) at temperatures that bracket those expected during interim storage. In these tests, the lifetime of canisters containing vitrified waste glass stored in air was predicted. The measured thickness of the reaction layer between the canister alloy and the canister alloy-environment, similar to that expected during interim storage, was extrapolated to estimate the time required for penetration of the 3/8-in thick canister.

Data from tests indicate that a 3/8-in. thick canister of type 304L stainless steel would not be penetrated for more than 8,000 years in a surface facility. By contrast, a 3/8-in. thick low carbon steel canister would be penetrated by oxidation in about 200 years of storage in a surface facility, and its strength would be reduced in a much shorter period.

Differences in canister lifetimes, predicted from these tests, are attributable to the differences in corrosion resistance of the candidate alloys. Both type 304L stainless steel and low carbon steel react similarly with vitrified waste, but type 304L stainless steel is much more resistant to both high temperature and atmospheric corrosion in a radiation field than is low carbon steel. The lifetime of canisters constructed from other compositions of austenitic stainless steels would be expected to be similar to that of type 304L.

Stainless steel has the additional advantage of forming a relatively thin oxide layer when heated by the molten glass. Tests made at Pacific Northwest Laboratory (PNL) indicate that an inert gas blanket would have to be used with a carbon steel canister to reduce the oxide scale formation to less than 22 lb per canister. Furthermore, the stainless steel surface is much easier to decontaminate by blasting with a frit-water slurry than is carbon steel.

The 3/8 in. nominal wall thickness of a 24-in. OD, schedule 20, stainless steel pipe is adequate for DWPF processing. A theoretical stress analysis was made on the reference canister just after it was filled with glass at the instantaneous pour rate of 3.8 lb/min. A maximum wall temperature of 427°C and a maximum bottom temperature of 649°C were assumed. The calculations show that the wall is sufficiently thick to permit the canister to be picked up immediately after it is filled, despite the residual shell hoop stress of 32,500 psi caused by the lower coefficient of thermal expansion of glass compared to that of stainless steel. Furthermore, the hoop stress quickly drops to about

5000 psi (at 500°C) due to the glass moving up into the canister void space as it gradually cools. Similarly, the thermal axial stresses were calculated to be 18,900 psi, and the simple static stresses due to weight were 477 psi shear and 177 psi axial. None of these stress levels indicates the need for a wall thickness greater than 3/8 in.

Canister Weight

The reference design canister is filled with approximately 165 gal of glass (22.1 ft^3) to a fill height of 91 in. This volume corresponds to a nominal weight of 3,700 lb for the current frit (Frit 200) and waste loading, and is about 85% of the available canister volume. The fill volume was chosen based upon operating experience where a 15% void is made available in the event of: low density foam partially filling the canister; "roping" of the glass stream causing voids in the frozen melt; and the possibility of spilling glass on the process room floor due to malfunction of load cells, level instrumentation, failure of pouring equipment, or operator error. After operating experience is gained, it may be possible to fill the canister to the top of the straight section of pipe at the intersection of the head with the cylinder. This volume is 25.3 ft^3 corresponding to a glass weight of 4,200 lb and a fill height of 104 in.

At the completion of pour, the centerline temperature at a point 37 in. from the canister base is about 750°C. At this temperature, the glass density can vary between 2.45 - 3.02 g/cm^3 corresponding to a glass fill weight ranging between 3,380 - 4,170 lb. The glass density variation is a function of the frit composition, waste loading, and waste composition.

Internal Pressurization Potential

Internal pressure within the canister is due to the accumulation of helium from alpha emissions of transuranic nuclides. A DWPF canister filled with waste glass produces about 0.32 cm^3 of helium per year at 40°C. The helium produced is assumed to diffuse through the glass into the void space above the solid glass surface. At the end of 1,000 years, the 103-liter void space pressure has increased by only 0.05 psi. This negligible pressure buildup is of no concern in waste package design. For the case of a canister filled to 25.3 ft^3 (733 L), the 23-liter void space pressure would increase by 0.2 psi.

Seal Weld

The reference process for sealing the canister is to resistance weld a 5-in. dia, 1/2-in. thick, type 304L stainless steel plug into the canister neck. A force of 75,000 lb, a current of 225,000 amps, and a voltage of approximately 10 volts is used to make the 1.5-sec weld. The technique was chosen after consideration of seven alternative processes including gas tungsten arc, gas metal arc, plasma arc, Thermit, electron beam, laser beam, and friction welding, because of the high weld quality and relatively simple equipment required. Weld tensile strength measurements were made on the upset resistance weld under varying conditions of oxidation to determine the need for machining the throat surface after the canister is filled with glass. An upset resistance weld with a 5-in. dia plug and a machine canister neck was leaktight to approximately 10^{-8} atm/cc/sec for a hydrostatic test pressure of 5,000 psi. If the canister neck is heated to 600°C, but not machined before it is welded, then the weld strength as measured by tensile and hydrostatic tests was reduced by about 20%. However, temperature measurements made on the canister neck during glass filling indicate that the maximum neck temperature does not exceed 300°C, so the canister seal weld is capable of withstanding at least 4,000 psi internal pressure while maintaining a leak tightness of 1×10^{-8} atm/cc/sec.

In the event that the canister is used in a repository with a flexible overpack and an open-ended sleeve, the canister could be subjected to relatively high lithostatic or hydrostatic pressures. The maximum pressure in a repository is expected to be less than 18 MPa (2,610 psi) which will buckle the 3/8-in. canister head above the glass melt surface. To prevent buckling, the head could have supporting ribs welded to the head interior or a thicker spherical head could be used. Present repository designs use rigid overpacks which are capable of withstanding repository pressures without collapsing.

Canister Decay Heat and Activity

Table 19 and Figure 15 describe the canister decay heat as a function of time for sludge-precipitate glass over a period of 5 to 1,000 years. The starting point is a sludge age of 5 years combined with precipitate from 15-year-old supernate. Figure 16 shows the canister activity for the same period. After a period of 300 years, the decay heat has decreased to about 7 watts and the activity to about 400 curies.

Fissionable Material Content

The fissionable material content of a sludge-precipitate glass canister is nominally 297 grams for sludge cooled 5 years, and for supernate cooled 15 years. Distribution of the thermal neutron fissionable nuclides is summarized below:

Fissionable Isotopes in One Canister

	g/lb of glass	g/can.[a]
U-233	4.43E-08	-
U-235	1.96E-02	73
Pu-239	5.61E-02	208
Pu-241	4.46E-03	16
		297

[a] Based on 3,700 lb of glass.

A nuclear criticality safety assessment was made for the DWPF glass melter and for storage of canisters in the interim storage building. The infinite neutron multiplication factor (k_∞) was calculated for two concentrations of Pu-239 and U-235 for the melter and the storage building.

Neutron Multiplication Factor (k_∞) as a Function of Fissionable Isotopes in Melter and Interim Storage Building

	Melter		Interim Storage Building	
	Conc 1	Conc 2	Conc 3	Conc 4
Pu-239[a]	1,120	560	280	140
U-235[a]	4,030	2,015	1,000	500
k_∞	0.110	0.063	0.012	0.008

[a] Grams of isotope in 3,650 lbs. of glass

Canister Gamma and Neutron Radiation

Canister radiation as a function of distance for glass containing 5-year-old sludge and precipitate from 15-year-old supernate is described in Table 20. The chemical composition of the glass waste is described in Table 21, the uranium and transuranic radionuclide content in Table 22, and a list of the major contributing isotopes to the gamma dose rate in Table 23.

Table 24 compares calculations of DWPF canister gamma radiation by four different codes and companies. The SRP calculation was made using the "ANISN" and "QAD" codes, the GA Technologies calculation was made using the "PATH" code, the Westinghouse calculation using the "SCAP" and ANISN-W codes, and Bechtel Inc., calculation using GRACE-II. ANISN is a one dimensional discrete ordinate code, the other three codes use point kernal integration techniques. All calculations were made using similar waste glass formulations.

Table 25 compares calculations of DWPF canister neutron radiation by three different methods. Although the calculations differ by a factor of less than 2, the contribution to the total radiation emitted from the canister is only 0.25 - 0.42 rem/hr.

Canister Surface Contamination

The criteria selected for canister surface contamination levels are identical to those specified for Department of Transportation cask shipping limits and are useful guides for canister decontamination by the frit-water slurry blasting technique. Canisters decontaminated to these levels are not expected to significantly contribute to air contamination within the Interim Storage Building. The canister surface contamination limits selected are:

Alpha	220 d/min/100 cm^2
Beta-Gamma	2200 d/min/100 cm^2

Labeling

Each canister will have a letter and four numbers located on the side wall and top head. The letter and numbers will be approximately 2 in. high and will be visible by television viewing. Each number will permit identification of the canister fabrication and processing history.

Canister Temperature

Table 26 describesthe temperature of a canister containing a sludge-precipitate wasteform at power levels of 425 to1000 watts, when in air at temperatures of 20°C and 38°C. Surface temperature of a 690-watt canister is estimated to be 58°C for an air temperature of 38°C. The centerline temperature is estimated to be 89°C for 38°C air temperature.

ESTIMATED PRODUCTION SCHEDULE

The power level and activity of canisters produced in the DWPF as a function of time is dependent on the mixing logistics of sludge, salt, and supernate in the waste tank farm.. In general, the intent is to remove waste from the oldest tanks first, since these tanks also contain the oldest waste. There are, however, practical constraints which limit the flexibility of transfer between areas, as well as between tanks, so that the present sludge inventory is segregated by processing area, somewhat segregated by type (HAW or LAW) and partially segregated by age.

The waste tank sludge and supernate blending schedule continues to be developed and refined. The preliminary schedule was described in "Characteristics of Spent Fuel, High-Level Waste, and Other Radioactive Wastes Which May Require Long-Term Isolation", DOE/RW-0184, December 1987. The data shown in Tables 27 and 28 of this report were developed from the 1988 Integrated Database and updates and the OCRWM December 1987 report. Since the Integrated Database information is developed each year, the schedule will become more accurate as hot startup is approached.

A description of the SRP waste inventory projected to the end of calendar year 1988 is shown in Table 27. At that time, the expected waste volume will be about 127,000 m^3 (33.4 million gallons), contains 778 million curies, and generates 2,300 kilowatts. Contributions of the principal fission product radionuclides are also shown. Of the total, Sr-90/Y-90 and Cs-137/Ba-137 contibute 70% of the total curies.

Table 28 describes the average radioactivity and thermal power per canister of waste glass as a function of time. The table covers the period from 1991 to 2022. Although the table reflects the best estimate of the schedule as of December 1988, it does not necessarily represent the actual processing schedule and tankage allocations; consequently, the data should be updated each year as the radioactive startup date approaches.

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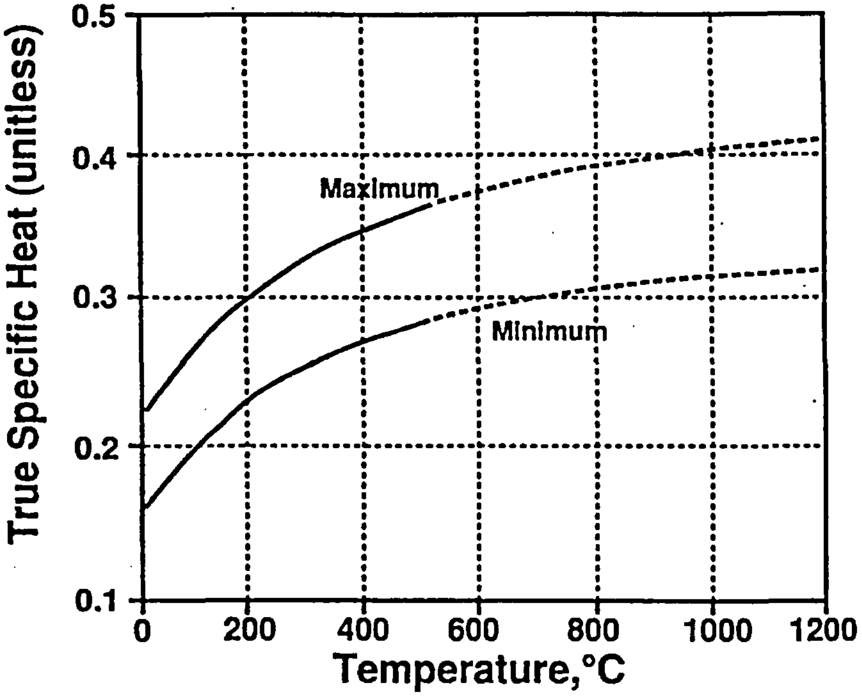

Figure 1. Calculated True Specific Heat of DWPF Glass

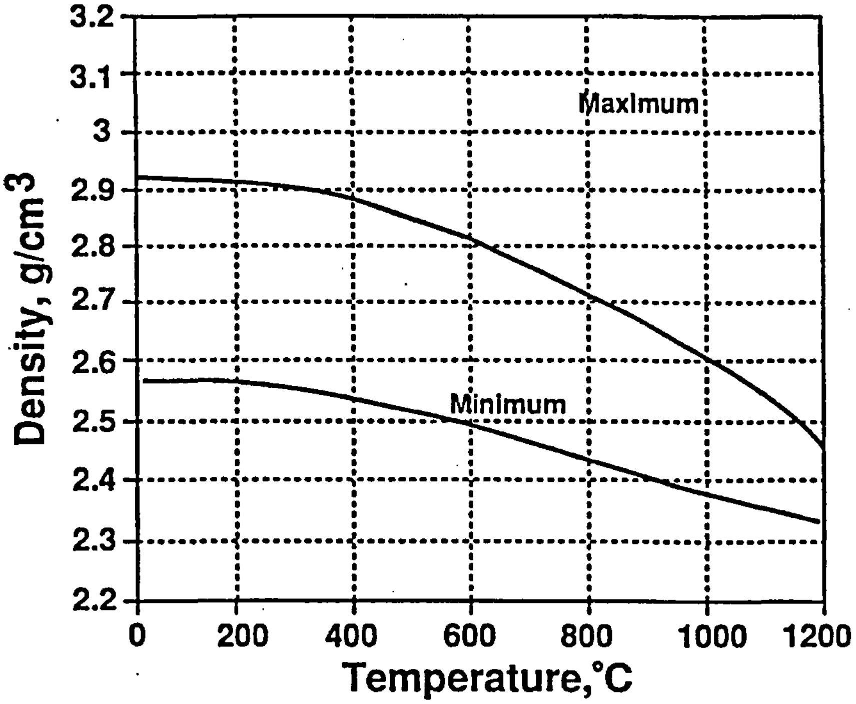

Figure 2. Calculated Density of DWPF Glass

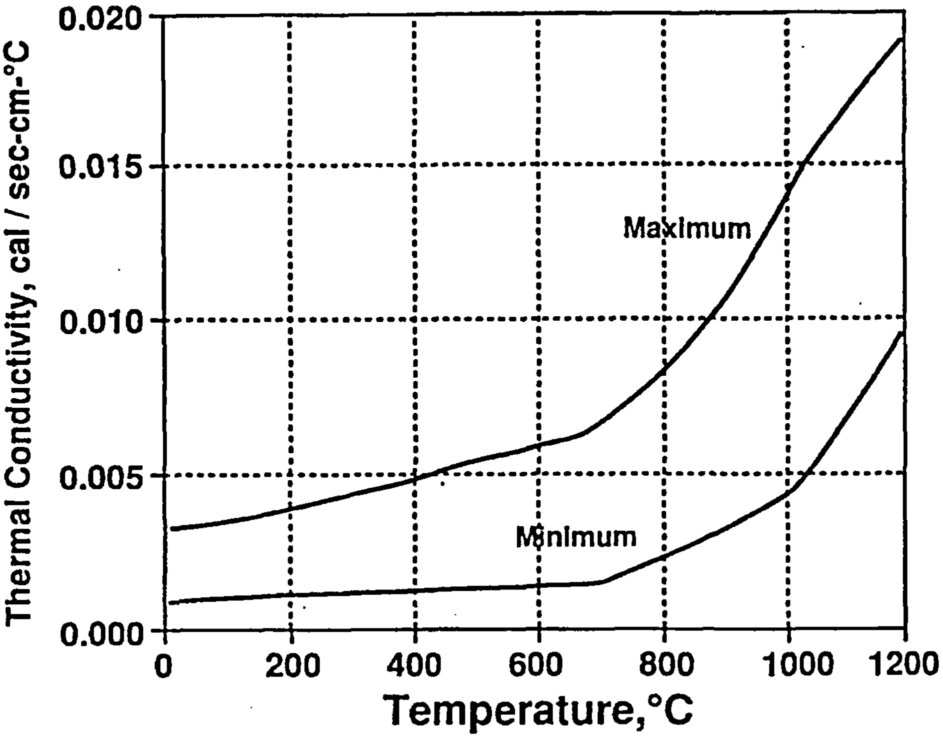

Figure 3. Calculated Thermal Conductivity of DWPF Glass

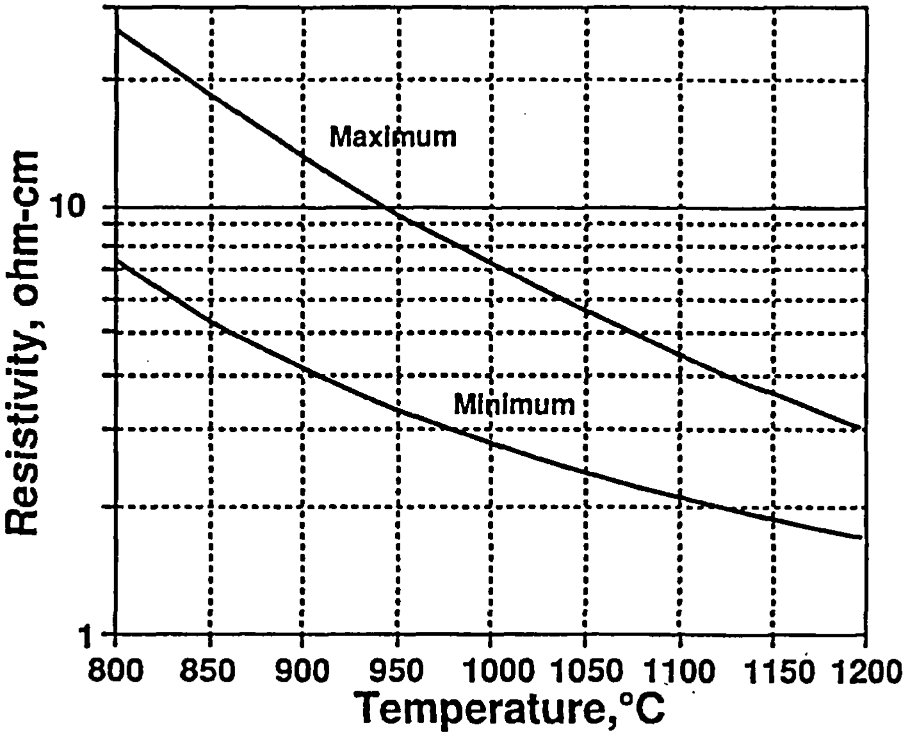

Figure 4. Experimental Resistivity of DWPF Glass

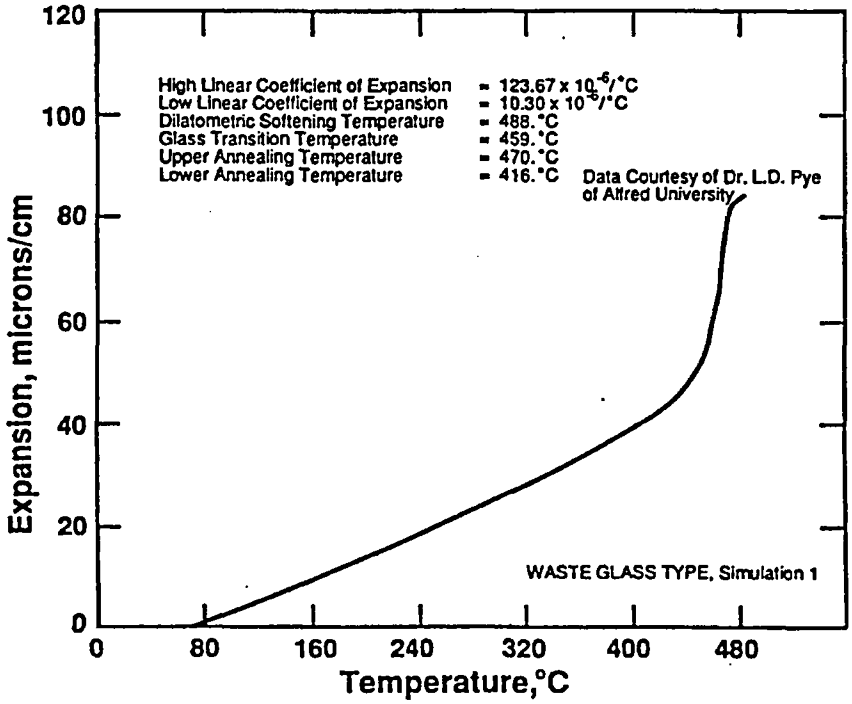

Figure 5. Experimental Thermal Expansion Data for Simulated Waste Glass, Simulation 1

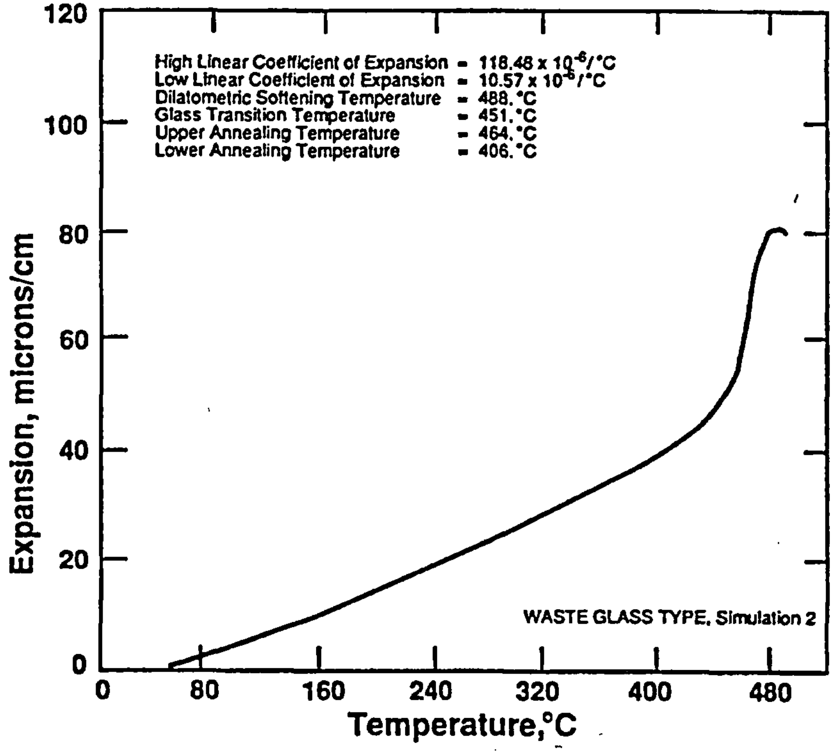

Figure 6. Experimental Thermal Expansion Data for Simulated Waste Glass, Simulation 2

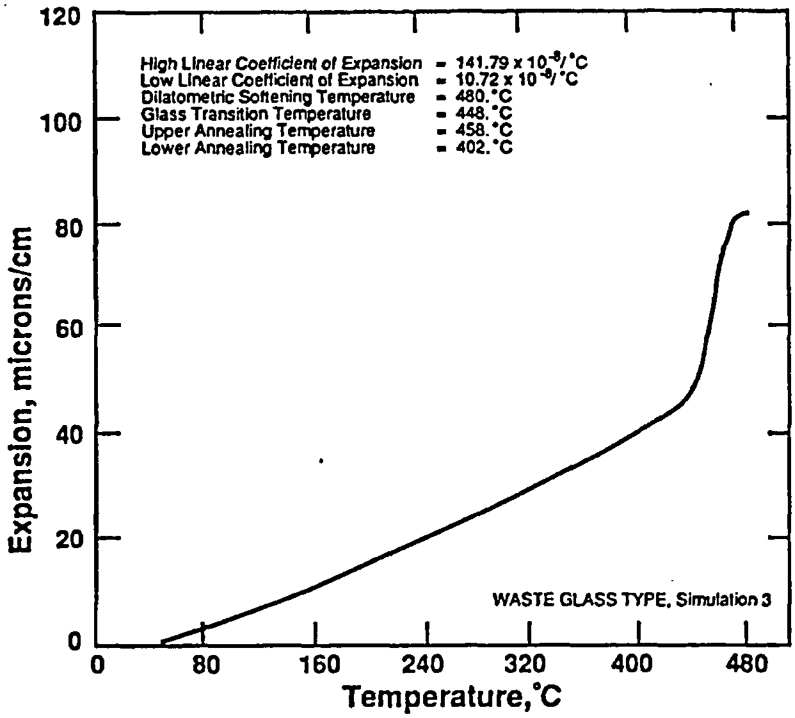

Figure 7. Experimental Thermal Expansion Data for Simulated Waste Glass, Simulation 3

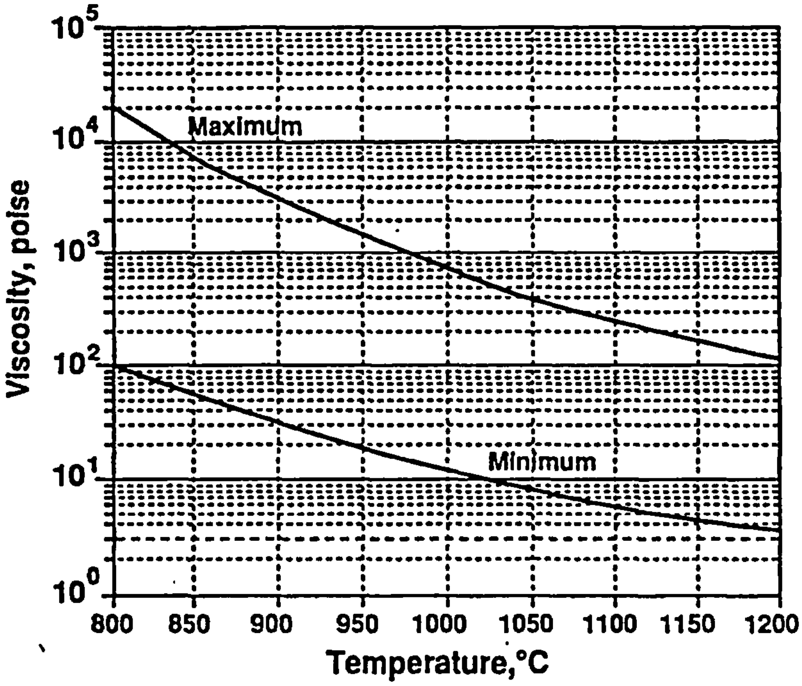

Figure 8. Experimental Viscosity of DWPF Glass

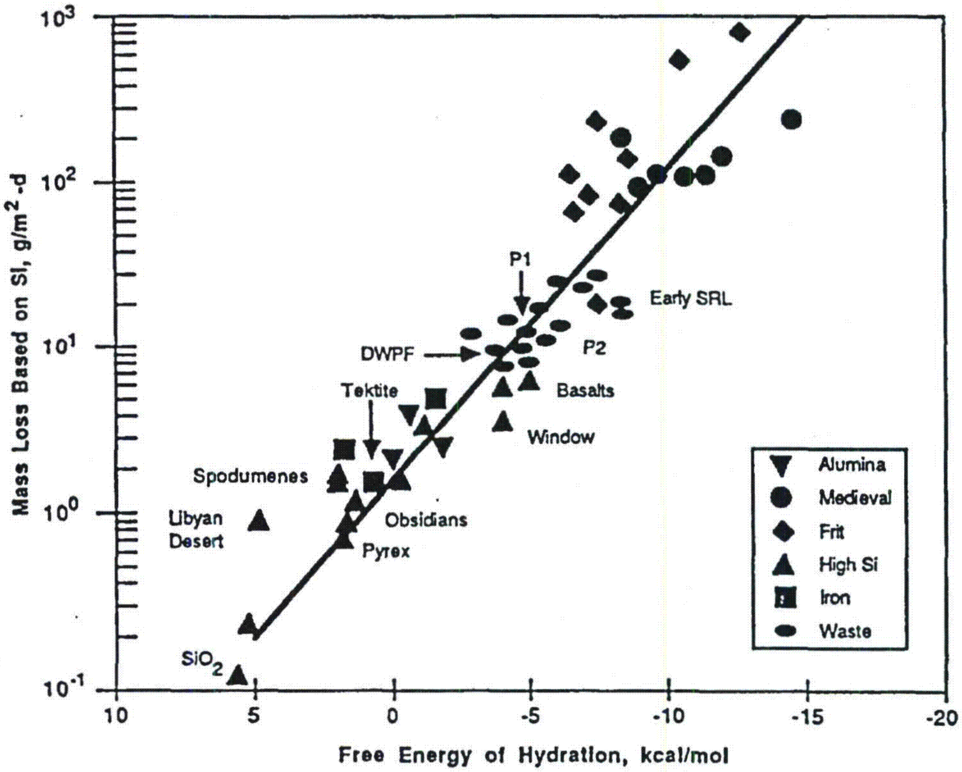

Figure 9. Durability of Glasses and Minerals in an MCC-1 Leach Test. Glasses were Exposed for 28 Days in 90°C Deionized Water.

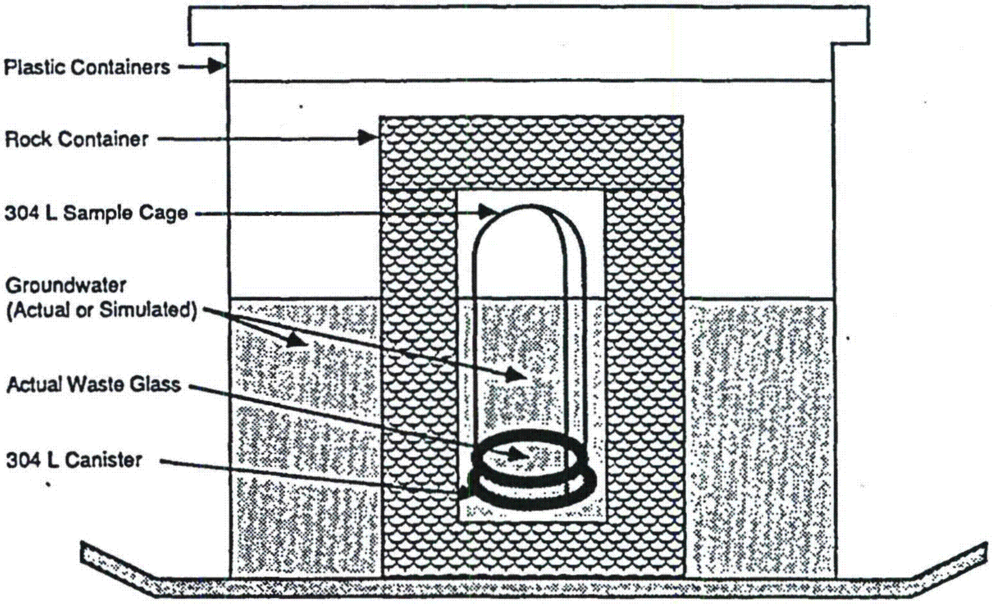

Figure 10. Repository Simulation Test Vessels

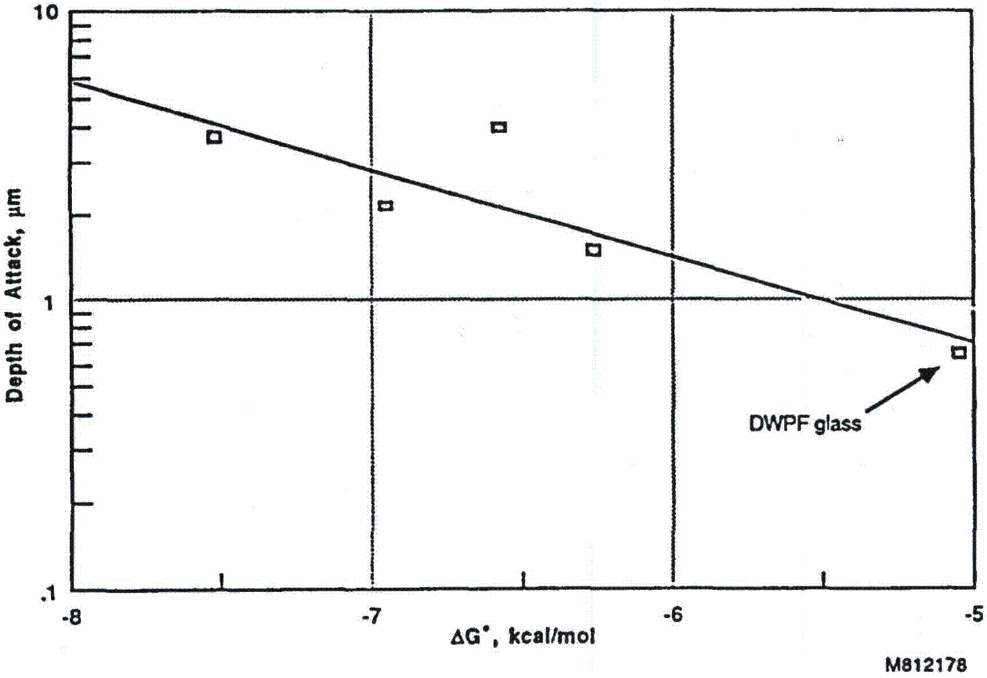

Figure 11. Results from 2-Year Burial Experiments

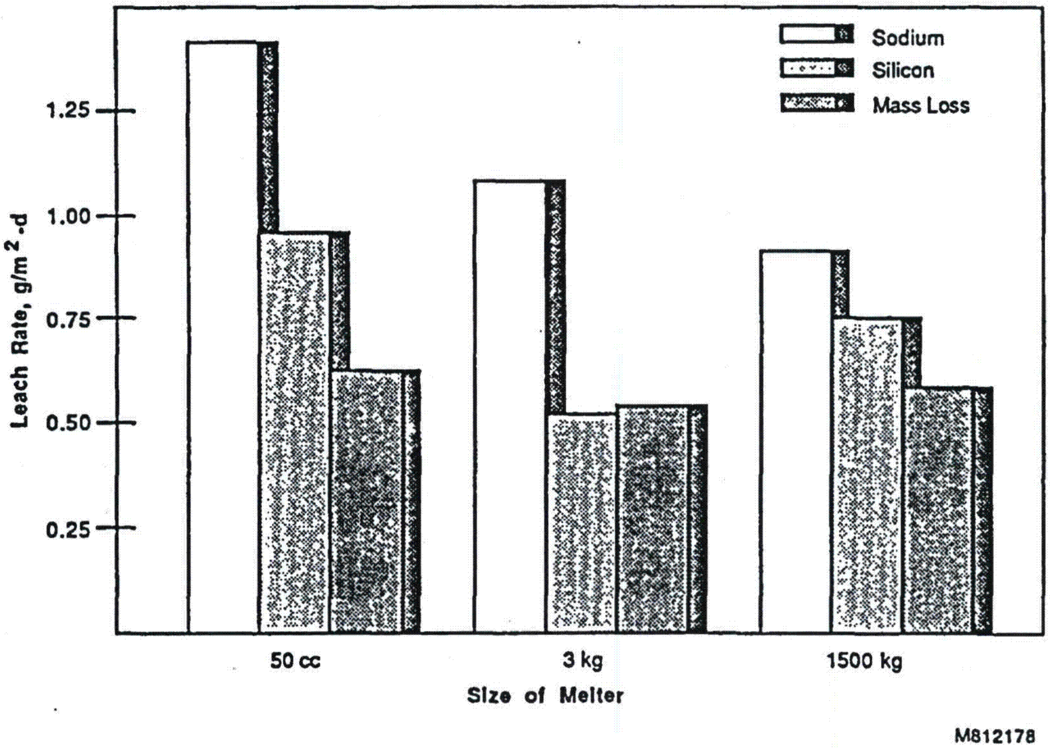

Figure 12. Effect of Melter Size on Glass Leaching

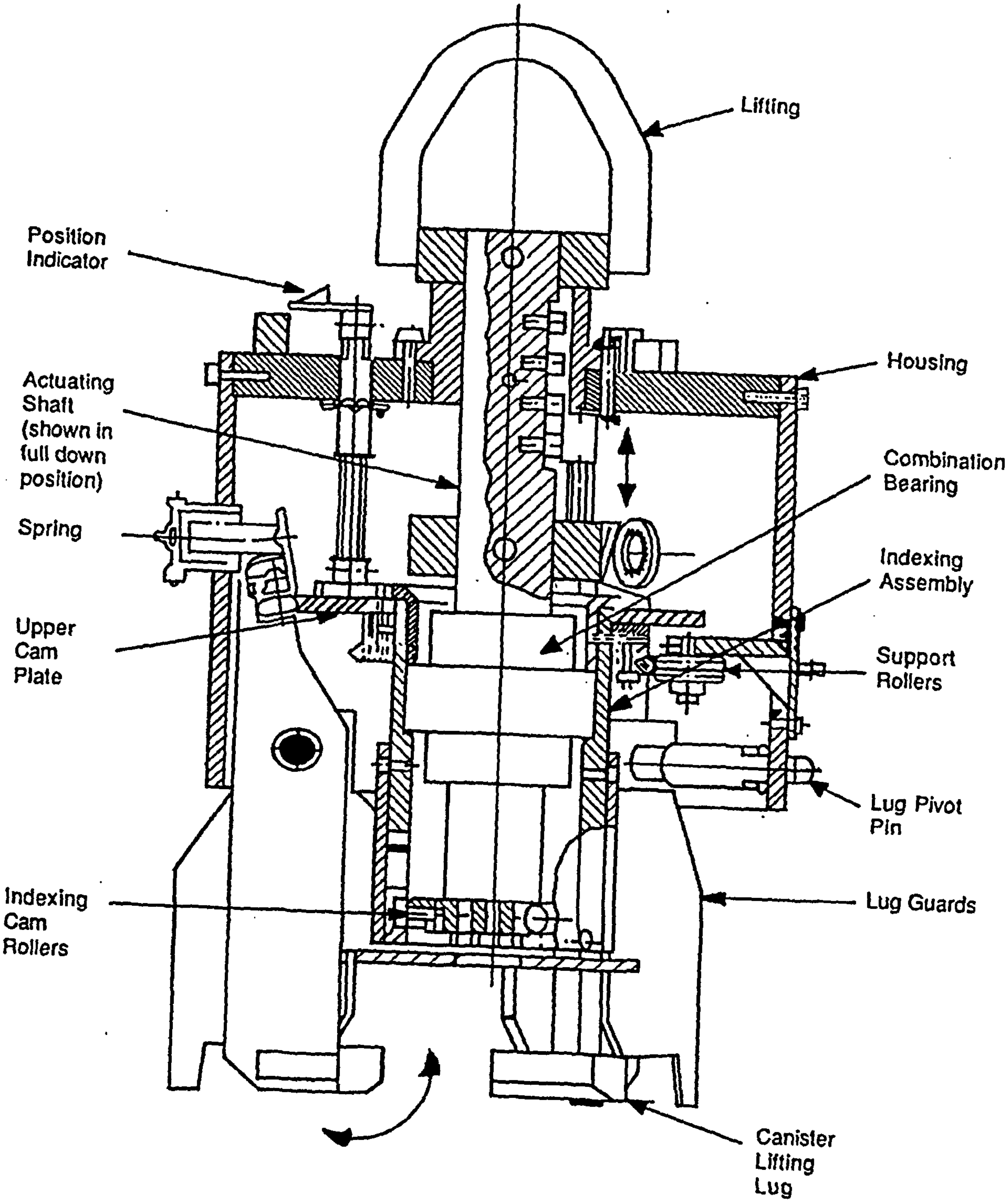

Figure 13. Canister Grapple

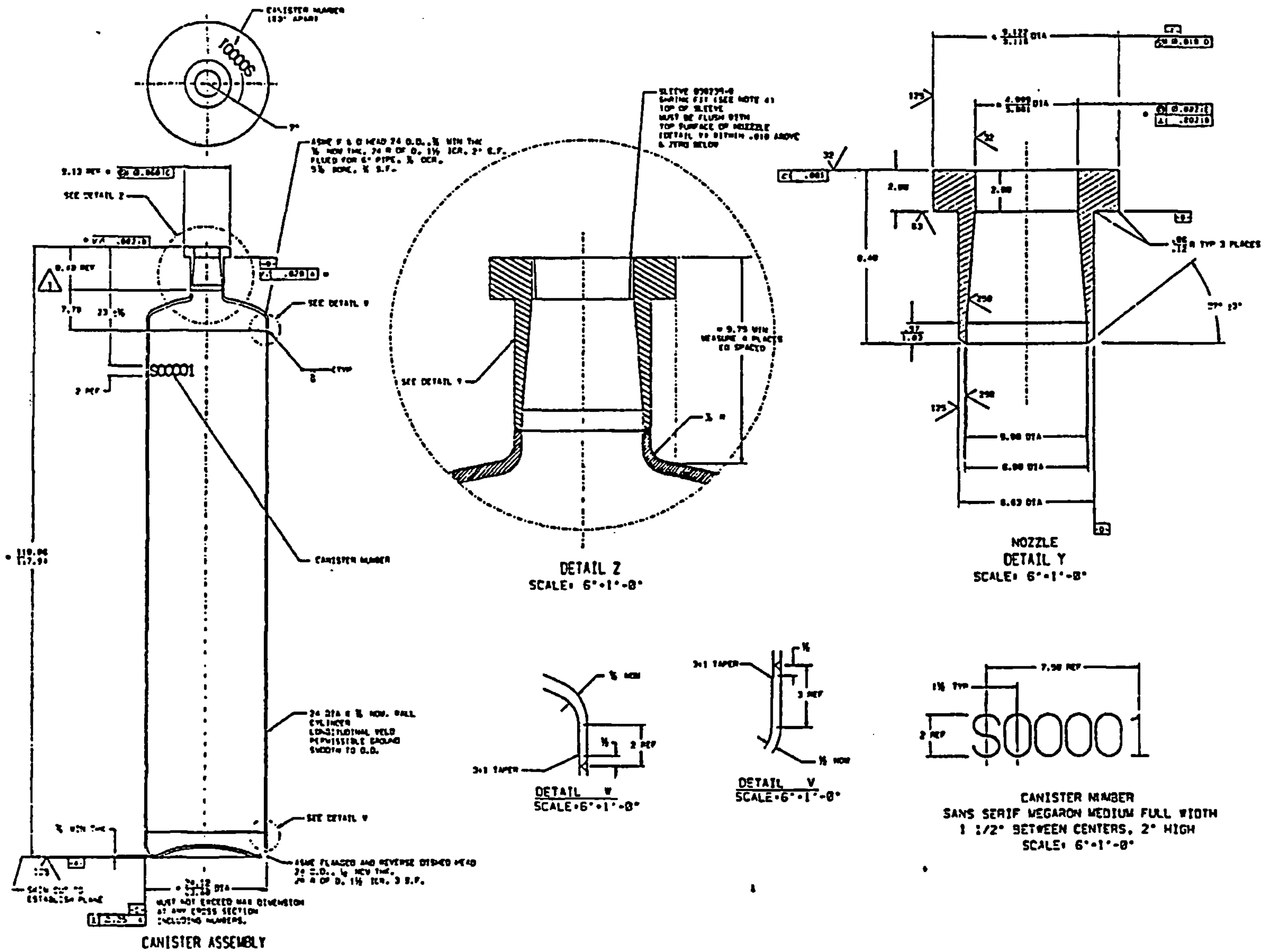

Figure 14. Canister Assembly

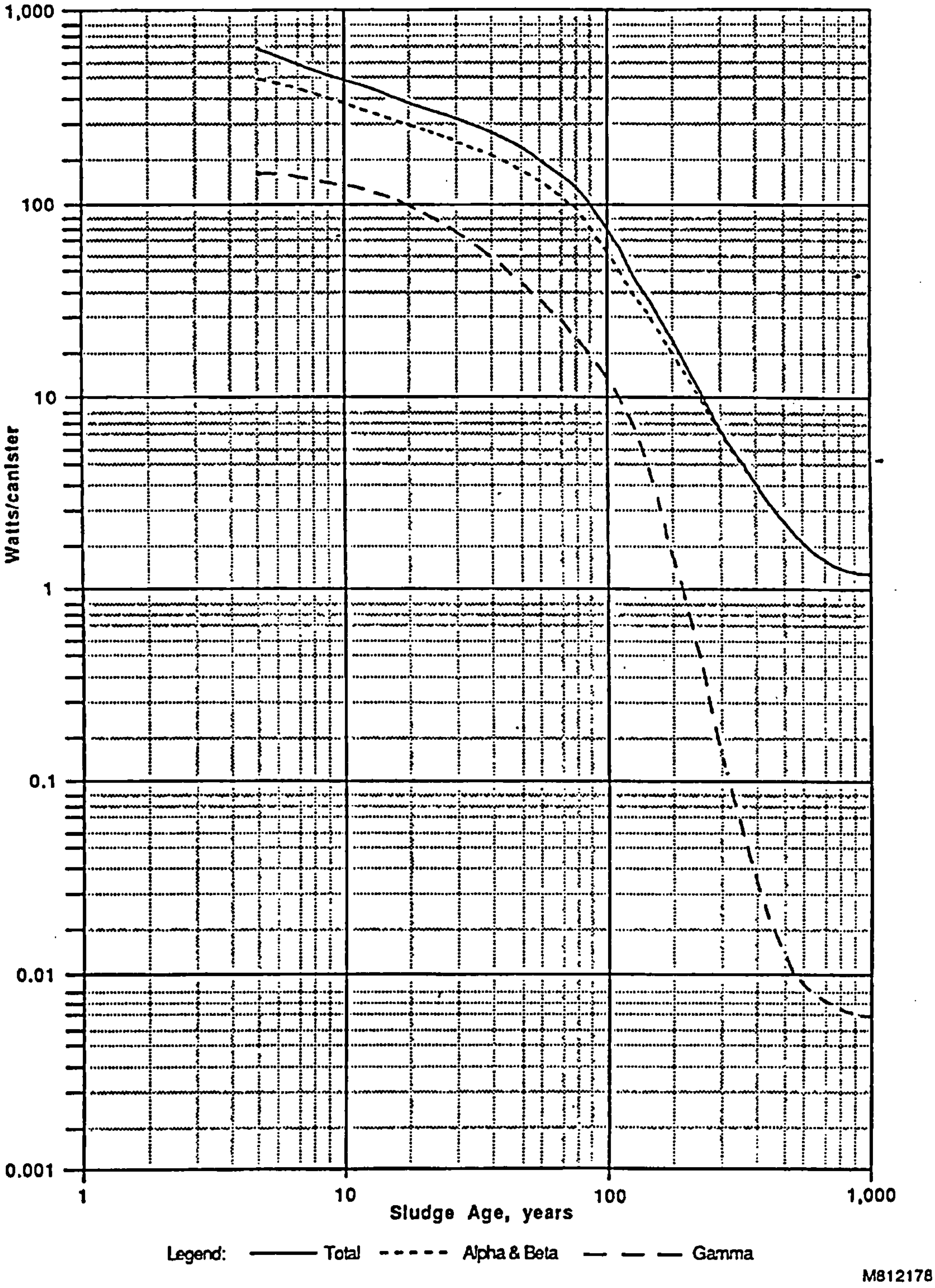

Figure 15. Sludge-Precipitate Canister Decay Heat

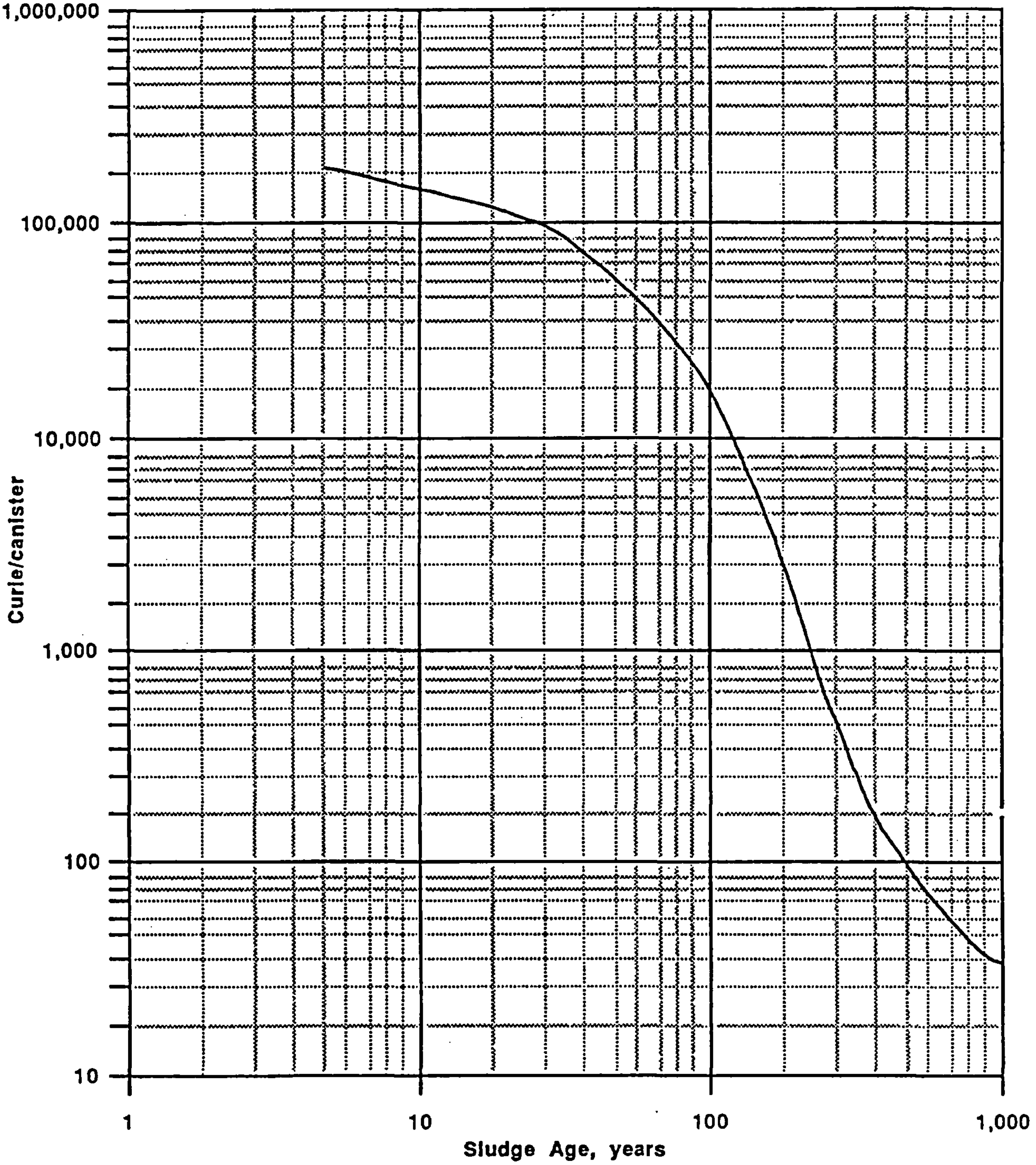

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Figure 16. Sludge-Precipitate Canister Activity

Table 1A

Chemical Composition of Sludge Feed Soluble Solids (Dry Basis)

Component	wt%	Component	wt%
$Ba(NO_3)_2$	0.649E-02	Na_2SO_4	0.492E+01
$CaSO_4$	0.642E-03	NA_2SiO_3	0.116
$CsNO_3$	0.716E-02	NA_3PO_4	0.349
Group A[a]	0.390E-02	$NaAg(OH)_2$	0.191E-03
KNO_3	0.500	$NaAl(OH)_4$	0.107E+02
NH_4NO_3	0.199E-01	NaCl	0.307
Na[(HgO)(OH)]	0.829E-02	NaF	0.154
$Na_2C_2O_4$	0.267	NaI	0.372E-03
Na_2CO_3	0.432E+01	$NaNO_2$	0.194E+02
Na_2CrO_4	0.133	$NaNO_3$	0.406E+02
Na_2MoO_4	0.219E-01	NaOH	0.182E+02
Na_2RhO_4	0.552E-03	$UO_2(OH)_2$	0.302E-04
Na_2RuO_4	0.237E-02		

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[a] Cd, Mo, Rb, Se, Tc, and Te.

Table 1B

Chemical Composition of Sludge Feed Insoluble Solids (Dry Basis)

Component	wt%	Component	wt%
AgOH	0.168	Na_2SO_4	0.132
$Al(OH)_3$	0.157E+02	Na_3PO_4	0.121E-01
$BaSO_4$	0.386	NaCl	0.110E+01
$Ca_3(PO_4)_2$	0.189	NaF	0.965E-01
CaC_2O_4	0.878	NaI	0.161E-01
$CaCO_3$	0.346E+01	$NaNO_3$	0.232E+01
CaF_2	0.151	NaOH	0.403E+01
$CaSO_4$	0.281	$Ni(OH)_2$	0.320E+01
Carbon	0.125	$PbCO_3$	0.114
$Co(OH)_3$	0.109E-01	$PbSO_4$	0.280
$Cr(OH)_3$	0.443	Pd(OH)2	0.622E-01
$CsNO_3$	0.124E-01	PuO_2	0.577E-01
$Cu(OH)_2$	0.159	RhO_2	0.262E-01
$Fe(OH)_3$	0.405E+02	RuO_2	0.134
Group A[a]	0.240	SiO_2	0.308E+01
Group B[b]	0.104E+01	$SrCO_3$	0.177
HgO	0.141E+01	ThO_2	0.567
KNO_3	0.536	$UO_2(OH)_2$	0.673E+01
$Mg(OH)_2$	0.400	$Y_2(CO_3)_3$	0.688E-01
MnO_2	0.691E+01	Zeolite	0.453E+01
		$Zn(OH)_2$	0.270

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[a] Cd, Mo, Rb, Se, Tc, and Te.

[b] Ag, Am, Ce, Cm, Co, Cr, Eu, La, Nb, Nd, Np, Pm, Pr, Sb, Sm, Sn, Tb, Tl, and Zr.

Table 2

Radionuclide Content of Sludge Feed

Isotope	Ci/Gal	Isotope	Ci/Gal	Isotope	Ci/Gal
H-3	1.93E-05	Sb-126m	1.28E-04	Eu-154	5.48E-01
C-14	3.21E-08	Te-125m	2.56E-01	Eu-155	4.21E-01
Cr-51	8.24E-20	Te-127	1.12E-04	Eu-156	4.64E-35
Co-60	1.50E-01	Te-127m	1.14E-04	Tb-160	9.91E-10
Ni-59	2.08E-05	Te-129	2.84E-15	Tl-208	9.70E-07
Ni-63	2.58E-03	Te-129m	4.44E-15	U-232	1.17E-05
Se-79	1.58E-04	I-129	1.31E-08	U-233	1.38E-09
Rb-87	5.55E-10	Cs-134	1.41E-01	U-234	2.98E-05
Sr-89	3.72E-08	Cs-135	2.47E-06	U-235	1.37E-07
Sr-90	4.05E+01	Cs-136	4.26E-43	U-236	9.80E-07
Y-90	4.16E+01	Cs-137	1.34E+00	U-238	9.14E-06
Y-91	6.57E-07	Ba-136m	7.52E-42	Np-236	1.52E-11
Zr-93	9.90E-04	Ba-137m	1.28E+00	Np-237	7.74E-06
Zr-95	8.90E-06	Ba-140	8.95E-40	Pu-236	1.07E-04
Nb-94	8.39E-08	La-140	3.83E-40	Pu-237	7.81E-15
Nb-95	1.89E-05	Ce-141	3.18E-14	Pu-238	1.30E+00
Nb-95m	1.10E-07	Ce-142	8.45E-09	Pu-239	1.13E-02
Tc-99	2.78E-03	Ce-144	8.74E+00	Pu-240	7.59E-03
Ru-103	1.50E-11	Pr-143	1.06E-37	Pu-241	1.46E+00
Ru-106	2.00E+00	Pr-144	8.74E+00	Pu-242	1.07E-05
Rh-103m	1.46E-11	Pr-144m	1.04E-01	Am-241	9.47E-03
Rh-106	2.01E+00	Nd-144	4.27E-13	Am-242	1.26E-05
Pd-107	1.27E-05	Nd-147	1.12E-47	Am-242m	1.26E-05
Ag-110m	1.10E-04	Pm-147	2.14E+01	Am-243	5.06E-06
Cd-113	4.64E-17	Pm-148	6.16E-14	Cm-242	3.09E-05
Cd-115m	1.13E-12	Pm-148m	8.93E-13	Cm-243	4.88E-06
Sn-121m	2.54E-05	Sm-147	1.73E-09	Cm-244	9.40E-02
Sn-123	2.26E-04	Sm-148	5.02E-15	Cm-245	5.84E-09
Sn-126	1.29E-04	Sm-149	1.55E-15	Cm-246	4.66E-10
Sb-124	6.31E-11	Sm-151	2.16E-01	Cm-247	5.72E-16
Sb-125	7.34E-01	Eu-152	3.26E-03	Cm-248	5.98E-16
Sb-126	1.80E-05				

Total actvity	1.33E+02 Ci/Gal
Decay heat	
Total primary	4.21E-01 Watt/Gal
Total gammas	1.92E-02 Watt/Gal

Table 3

Partial Isotopic Content of Sludge Feed

Isotope	G/Gal	Isotope	G/Gal	Isotope	G/Gal
H-3	2.01E-09	Ru-104	7.21E-02	Te-125	2.86E-03
C-14	7.21E-09	Ru-106	5.98E-04	Te-125m	1.42E-05
Cr-51	8.92E-25	Rh-103	7.52E-02	Te-126	1.25E-04
Co-60	1.33E-04	Rh-103m	4.48E-19	Te-127	4.23E-11
Ni-59	2.57E-04	Rh-106	5.64E-10	Te-127m	1.21E-08
Ni-63	4.37E-05	Pd-104	9.85E-03	Te-128	3.41E-02
Se-77	3.66E-04	Pd-105	7.85E-02	Te-129	1.36E-22
Se-78	9.19E-04	Pd-106	5.06E-02	Te-129m	1.47E-19
Se-79	2.26E-03	Pd-107	2.46E-02	Te-130	1.23E-01
Se-80	5.40E-03	Pd-108	1.45E-02	I-127	1.99E-05
Se-82	1.09E-02	Pd-110	5.04E-03	I-129	7.40E-05
Rb-85	2.58E-03	Ag-109	5.13E-03	Cs-133	1.69E-02
Rb-87	6.34E-03	Ag-110m	2.32E-08	Cs-134	1.09E-04
Sr-88	2.02E-01	Cd-110	6.18E-04	Cs-135	2.14E-03
Sr-89	1.28E-12	Cd-111	2.91E-03	Cs-136	5.75E-48
Sr-90	2.97E-01	Cd-112	2.01E-03	Cs-137	1.54E-02
Y-89	1.37E-01	Cd-113	1.36E-04	Ba-134	2.89E-02
Y-90	7.65E-05	Cd-114	3.26E-03	Ba-136	3.06E-03
Y-91	2.68E-11	Cd-115m	4.42E-17	Ba-136m	2.77E-53
Zr-90	2.78E-02	Cd-116	1.51E-03	Ba-137	1.18E-01
Zr-91	2.21E-01	Sn-116	2.09E-04	Ba-137m	2.38E-09
Zr-92	2.27E-01	Sn-117	1.08E-03	Ba-138	1.05E+00
Zr-93	3.93E-01	Sn-118	1.16E-03	Ba-140	1.23E-44
Zr-94	2.51E-01	Sn-119	1.13E-03	La-139	3.80E-01
Zr-95	4.15E-10	Sn-120	1.17E-03	La-140	6.88E-46
Zr-96	2.52E-01	Sn-121m	4.72E-07	Ce-140	3.74E-01
Nb-94	4.48E-07	Sn-122	1.31E-03	Ce-141	1.12E-18
Nb-95	4.81E-10	Sn-123	2.75E-08	Ce-142	3.52E-01
Nb-95m	2.90E-13	Sn-124	1.96E-03	Ce-144	2.74E-03
Mo-95	2.52E-01	Sn-125	1.58E-61	Pr-141	3.51E-01
Mo-96	1.02E-03	Sn-126	4.54E-03	Pr-143	1.58E-42
Mo-97	2.40E-01	Sb-121	1.26E-03	Pr-144	1.16E-07
Mo-98	2.48E-01	Sb-123	1.59E-03	Pr-144m	5.76E-10
Mo-100	2.65E-01	Sb-124	3.60E-15	Nd-142	1.25E-03
Tc-99	1.64E-01	Sb-125	7.11E-04	Nd-143	4.19E-01
Ru-100	1.67E-03	Sb-126	2.15E-10	Nd-144	3.60E-01
Ru-101	1.94E-01	Sb-126m	1.63E-12	Nd-145	2.32E-01
Ru-102	1.43E-01	Te-122	2.69E-05	Nd-146	1.88E-01
Ru-103	4.65E-16	Te-124	1.25E-05	Nd-147	1.39E-52
Nd-148	1.09E-01	Eu-156	8.41E-40	Pu-238	7.57E-02
Nd-150	4.35E-02	Tb-159	1.81E-04	Pu-239	1.82E-01
Pm-147	2.31E-02	Tb-160	8.77E-14	Pu-240	3.34E-02
Pm-148	3.75E-19	Tl-206	1.99E-29	Pu-241	1.44E-02

Table 3 Contd

Partial Isotopic Content of Sludge Feed

Isotope	G/Gal	Isotope	G/Gal	Isotope	G/Gal
Pm-148m	4.18E-17	Tl-207	3.28E-19	Pu-242	2.73E-03
Sm-147	7.42E-02	Tl-208	3.29E-15	Am-241	2.76E-03
Sm-148	1.65E-02	Tl-209	9.63E-24	Am-242	1.55E-11
Sm-149	6.43E-03	U-232	5.42E-07	Am-242m	1.30E-06
Sm-150	9.13E-02	U-233	1.43E-07	Am-243	2.54E-05
Sm-151	8.19E-03	U-234	4.77E-03	Cm-242	9.32E-09
Sm-152	3.29E-02	U-235	6.33E-02	Cm-243	9.46E-08
Sm-154	5.85E-03	U-236	1.51E-02	Cm-244	1.16E-03
Eu-151	3.43E-04	U-238	2.72E+01	Cm-245	3.39E-08
Eu-152	1.84E-05	Np-236	1.16E-09	Cm-246	1.52E-09
Eu-153	1.81E-02	Np-237	1.10E-02	Cm-247	6.17E-12
Eu-154	2.03E-03	Pu-236	2.01E-07	Cm-248	1.41E-13
Eu-155	9.04E-04	Pu-237	6.47E-19		

Total 3.60E+01 G/Gal

Table 4

Chemical Composition of Precipitate Feed from In-Tank Processing to Salt Cell

Component	Water Free wt%
$Al(OH)_3$	0.47
CsTPB	0.79
$Fe(OH)_3$	0.49
$Hg(C_6H_5)_2$	0.88
KTPB	75.60
NH_4TPB	3.54
$Na_2C_2O_4$	0.97
Na_2CO_3	0.62
Na_2SO_4	0.71
$NaAl(OH)_4$	1.31
$NaNO_2$	1.48
$NaNO_3$	5.96
NAOH	2.40
NaTBP	0.66
$NaTi_2O_5H$	3.52
Others	0.60
Total	100.00

Table 5

Radionuclide Content of Precipitate Slurry Feed to the Salt Cell

Isotope	Ci/Gal	Isotope	Ci/Gal	Isotope	Ci/Gal
H-3	9.06E-05	Sb-124	8.88E-30	Tb-160	2.64E-27
C-14	1.98E-09	Sb-125	1.50E-02	Tl-208	2.67E-08
Co-60	3.28E-04	Sb-126	3.22E-05	U-232	1.67E-07
Ni-59	2.25E-07	Sb-126m	2.30E-04	U-233	2.45E-11
Ni-63	2.72E-05	Te-125m	2.15E-07	U-234	8.35E-07
Se-79	3.75E-07	Te-127	9.73E-20	U-235	1.48E-09
Rb-87	2.25E-10	Te-127m	9.94E-20	U-236	1.07E-08
Sr-89	7.92E-32	I-129	1.51E-10	U-238	9.91E-08
Sr-90	3.99E-01	Cs-134	1.66E-01	Np-236	1.96E-13
Y-90	4.12E-01	Cs-135	8.37E-05	Np-237	1.00E-07
Y-91	4.93E-28	Cs-137	3.60E+01	Pu-236	1.20E-07
Zr-93	4.08E-07	Ba-137m	3.44E+01	Pu-237	8.04E-41
Zr-95	6.26E-26	Ce-141	2.17E-50	Pu-238	1.53E-02
Nb-94	9.10E-10	Ce-142	3.63E-11	Pu-239	1.44E-04
Nb-95	1.32E-25	Ce-144	5.11E-06	Pu-240	9.71E-05
Nb-95m	7.74E-28	Pr-144	5.13E-06	Pu-241	1.16E-02
Tc-99	6.97E-05	Pr-144m	6.13E-08	Pu-242	1.37E-07
Ru-103	3.85E-41	Nd-144	1.85E-15	Am-241	2.40E-04
Ru-106	3.14E-05	Pm-147	6.56E-03	Am-242	1.55E-07
Rh-103m	3.75E-41	Pm-148	6.31E-43	Am-242m	1.56E-07
Rh-106	3.15E-05	Pm-148m	9.16E-42	Am-243	6.52E-08
Pd-107	1.38E-07	Sm-147	3.35E-11	Cm-242	1.28E-07
Ag-110m	3.18E-10	Sm-148	7.56E-17	Cm-243	4.94E-08
Cd-113	5.23E-20	Sm-149	2.32E-17	Cm-244	1.20E-03
Cd-115m	2.81E-40	Sm-151	3.02E-03	Cm-245	7.53E-11
Sn-121m	3.93E-05	Eu-152	1.07E-05	Cm-246	6.00E-12
Sn-123	1.21E-12	Eu-154	1.37E-03	Cm-247	7.38E-18
Sn-126	2.30E-04	Eu-155	5.54E-04	Cm-248	7.73E-18

Total activity	7.14E+01 Ci/Gal
Decay heat	
Total primary	4.33E-02 Watt/Gal
Total gammas	1.24E-01 Watt/Gal

Table 6

Partial Isotopic Content of Precipitate Slurry Feed to the Salt Cell

Isotope	G/Gal	Isotope	G/Gal	Isotope	G/Gal
H-3	9.43E-09	Ru-106	9.38E-09	Te-126	1.33E-09
C-14	4.44E-10	Rh-103	9.90E-04	Te-127	3.68E-26
Co-60	2.90E-07	Rh-103m	1.15E-48	Te-127m	1.05E-23
Ni-59	2.79E-06	Rh-106	8.85E-15	Te-128	3.63E-07
Ni-63	4.61E-07	Pd-104	1.07E-04	Te-129	1.74E-60
Se-77	8.63E-07	Pd-105	8.53E-04	Te-129m	1.88E-57
Se-78	2.18E-06	Pd-106	5.62E-04	Te-130	1.31E-06
Se-79	5.37E-06	Pd-107	2.68E-04	I-127	2.31E-07
Se-80	1.28E-05	Pd-108	1.58E-04	I-129	8.56E-07
Se-82	2.58E-05	Pd-110	5.50E-05	Cs-133	5.76E-01
Rb-85	1.04E-03	Ag-109	1.03E-02	Cs-134	1.28E-04
Rb-87	2.57E-03	Ag-110m	6.68E-14	Cs-135	7.26E-02
Sr-88	2.54E-03	Cd-110	6.93E-07	Cs-137	4.15E-01
Sr-89	2.72E-36	Cd-111	3.26E-06	Ba-134	3.82E-04
Sr-90	2.92E-03	Cd-112	2.25E-06	Ba-136	3.38E-05
Y-89	1.49E-03	Cd-113	1.54E-07	Ba-137	3.23E-03
Y-90	7.57E-07	Cd-114	3.67E-06	Ba-137m	6.39E-08
Y-91	2.01E-32	Cd-115m	1.10E-44	Ba-138	1.15E-02
Zr-90	2.85E-05	Cd-116	1.69E-06	Ce-140	1.61E-03
Zr-91	9.12E-05	Sn-116	3.74E-04	Ce-141	7.61E-55
Zr-92	9.36E-05	Sn-117	1.94E-03	Ce-142	1.51E-03
Zr-93	1.62E-04	Sn-118	2.08E-03	Ce-144	1.60E-09
Zr-94	1.04E-04	Sn-119	2.02E-03	Pr-141	1.51E-03
Zr-95	2.92E-30	Sn-120	2.10E-03	Pr-144	6.78E-14
Zr-96	1.08E-04	Sn-121m	7.32E-07	Pr-144m	3.38E-16
Nb-94	4.85E-09	Sn-122	2.34E-03	Nd-142	5.37E-06
Nb-95	3.36E-30	Sn-123	1.47E-16	Nd-143	1.80E-03
Nb-95m	2.04E-33	Sn-124	3.51E-03	Nd-144	1.56E-03
Mo-95	2.30E-03	Sn-126	8.10E-03	Nd-145	9.95E-04
Mo-96	9.33E-06	Sb-121	3.27E-04	Nd-146	8.10E-04
Mo-97	2.20E-03	Sb-123	4.13E-04	Nd-148	4.67E-04
Mo-98	2.26E-03	Sb-124	5.07E-34	Nd-150	1.87E-04
Mo-100	2.42E-03	Sb-125	1.45E-05	Pm-147	7.07E-06
Tc-99	4.11E-03	Sb-126	3.85E-10	Pm-148	3.84E-48
Ru-100	2.50E-05	Sb-126m	2.93E-12	Pm-148m	4.28E-46
Ru-101	1.93E-03	Te-122	2.87E-10	Sm-147	1.44E-03
Ru-102	2.13E-03	Te-124	1.34E-10	Sm-148	2.48E-04
Ru-103	1.19E-45	Te-125	4.04E-08	Sm-149	9.66E-05
Ru-104	1.07E-03	Te-125m	1.19E-11	Sm-150	1.37E-03
Sm-151	1.15E-04	U-232	7.78E-09	Pu-241	1.15E-04
Sm-152	4.93E-04	U-233	2.54E-09	Pu-242	3.49E-05
Sm-154	8.78E-05	U-234	1.34E-04	Am-241	6.98E-05
Eu-151	5.22E-06	U-235	6.86E-04	Am-242	1.91E-13
Eu-152	6.05E-08	U-236	1.65E-04	Am-242m	1.60E-08

Table 6 Contd

Partial Isotopic Content of Precipitate Slurry Feed to the Salt Cell

Isotope	G/Gal	Isotope	G/Gal	Isotope	G/Gal
Eu-153	1.01E-04	U-238	2.95E-01	Am-243	3.27E-07
Eu-154	5.07E-06	Np-236	1.49E-11	Cm-242	3.88E-11
Eu-155	1.19E-06	Np-237	1.43E-04	Cm-243	9.57E-10
Tb-159	7.76E-07	Pu-236	2.26E-10	Cm-244	1.48E-05
Tb-160	2.34E-31	Pu-237	6.66E-45	Cm-245	4.37E-10
Tl-206	8.54E-30	Pu-238	8.92E-04	Cm-246	1.95E-11
Tl-207	1.17E-20	Pu-239	2.32E-03	Cm-247	7.96E-14
Tl-208	9.08E-17	Pu-240	4.27E-04	Cm-248	1.82E-15
Tl-209	5.46E-25				
	Total	1.46E+00 G/Gal			

Table 7

Chemical Composition of Feed from Salt Cell

Component	Water Free wt%
$(BC_6H_5O)_3$	0.91
$(C_6H_5)_2$	5.03
$Al(OH)_3$	0.76
$C_6H_5B(OH)_2$	6.07
$C_6H_5HgCOOH$	1.31
C_6H_5OH	2.07
CsCOOH	0.50
$Cu(COOH)_2$	1.33
$Fe(OH)_3$	0.80
H_3BO_3	19.02
HCOOH	1.36
KCOOH	28.86
NH_4COOH	1.08
$Na_2C_2O_4$	1.58
Na_2SO_4	2.39
$NaAl(OH)_4$	2.14
NaCOOH	13.10
$NaNO_3$	5.02
$NaTi_2O_5H$	5.72
Others	0.95
Total	100.00

Table 8

Radionuclide Content of Feed from Salt Cell

Isotope	Ci/Gal	Isotope	Ci/Gal	Isotope	Ci/Gal
H-3	9.57E-05	Sb-124	9.51E-30	Tb-160	2.83E-27
C-14	7.45E-11	Sb-125	1.61E-02	Tl-208	2.86E-08
Co-60	3.51E-04	Sb-126	3.45E-05	U-232	1.79E-07
Ni-59	2.41E-07	Sb-126m	2.46E-04	U-233	2.62E-11
Ni-63	2.92E-05	Te-125m	2.31E-07	U-234	8.94E-07
Se-79	4.01E-07	Te-127	1.04E-19	U-235	1.59E-09
Rb-87	2.41E-10	Te-127m	1.06E-19	U-236	1.14E-08
Sr-89	8.48E-32	I-129	1.62E-10	U-238	1.06E-07
Sr-90	4.27E-01	Cs-134	1.78E-01	Np-236	2.10E-13
Y-90	4.42E-01	Cs-135	8.96E-05	Np-237	1.08E-07
Y-91	5.27E-28	Cs-137	3.85E+01	Pu-236	1.28E-07
Zr-93	4.37E-07	Ba-137m	3.69E+01	Pu-237	8.61E-41
Zr-95	6.70E-26	Cs-141	2.32E-50	Pu-238	1.63E-02
Nb-94	9.75E-10	Ce-142	3.89E-11	Pu-239	1.54E-04
Nb-95	1.41E-25	Ce-144	5.47E-06	Pu-240	1.04E-04
Nb-95m	8.28E-28	Pr-144	5.49E-06	Pu-241	1.24E-02
Tc-99	7.46E-05	Pr-144m	6.57E-08	Pu-242	1.47E-07
Ru-103	4.13E-41	Nd-144	1.98E-15	Am-241	2.57E-04
Ru-106	3.36E-05	Pm-147	7.03E-03	Am-242	1.66E-07
Rh-103m	4.01E-41	Pm-148	6.76E-43	Am-242m	1.67E-07
Rh-106	3.37E-05	Pm-148m	9.80E-42	Am-243	6.98E-08
Pd-107	1.47E-07	Sm-147	3.59E-11	Cm-242	1.37E-07
Ag-110m	3.40E-10	Sm148	8.10E-17	Cm-243	5.29E-08
Cd-113	5.60E-20	Sm-149	2.48E-17	Cm-244	1.29E-03
Cd-115m	3.01E-40	Sm-151	3.23E-03	Cm-245	8.06E-11
Sn-121m	4.21E-05	Eu-152	1.14E-05	Cm-246	6.42E-12
Sn-123	1.30E-12	Eu-154	1.47E-03	Cm-247	7.90E-18
Sn-126	2.46E-04	Eu-155	5.93E-04	Cm-248	8.28E-18

Total activity	7.65E+01 Ci/Gal
Decay heat	
Total primary	4.63E-02 Watt/Gal
Total gammas	1.32E-01 Watt/Gal

Table 9

Partial Isotopic Content of Feed from Salt Cell

Isotope	G/Gal	Isotope	G/Gal	Isotope	G/Gal
H-3	9.97E-09	Ru-106	1.00E-08	Te-126	1.43E-09
C-14	1.67E-11	Rh-103	1.06E-03	Te-127	3.94E-26
Co-60	3.10E-07	Rh-103m	1.23E-48	Te-127m	1.13E-23
Ni-59	2.99E-06	Rh-106	9.47E-15	Te-128	3.89E-07
Ni-63	4.94E-07	Pd-104	1.15E-04	Te-129	1.86E-60
Se-77	9.24E-07	Pd-105	9.14E-04	Te-129m	2.01E-57
Se-78	2.33E-06	Pd-106	6.02E-04	Te-130	1.41E-06
Se-79	5.75E-06	Pd-107	2.87E-04	I-127	2.47E-07
Se-80	1.37E-05	Pd-108	1.69E-04	I-129	9.16E-07
Se-82	2.77E-05	Pd-110	5.89E-05	Cs-133	6.16E-01
Rb-85	1.12E-03	Ag-109	1.11E-02	Cs-134	1.37E-04
Rb-87	2.75E-03	Ag-110m	7.16E-14	Cs-135	7.77E-02
Sr-88	2.73E-03	Cd-110	7.42E-07	Cs-137	4.45E-01
Sr-89	2.91E-36	Cd-111	3.49E-06	Ba-134	4.09E-04
Sr-90	3.13E-03	Cd-112	2.41E-06	Ba-136	3.62E-05
Y-89	1.59E-03	Cd-113	1.64E-07	Ba-137	3.46E-03
Y-90	8.11E-07	Cd-114	3.93E-06	Ba-137m	6.85E-08
Y-91	2.15E-32	Cd-115m	1.18E-44	Ba-138	1.23E-02
Zr-90	3.05E-05	Cd-116	1.81E-06	Ce-140	1.72E-03
Zr-91	9.76E-05	Sn-116	4.00E-04	Ce-141	8.15E-55
Zr-92	1.00E-04	Sn-117	2.07E-03	Ce-142	1.62E-03
Zr-93	1.74E-04	Sn-118	2.22E-03	Ce-144	1.71E-09
Zr-94	1.11E-04	Sn-119	2.16E-03	Pr-141	1.61E-03
Zr-95	3.12E-30	Sn-120	2.25E-03	Pr-144	7.26E-14
Zr-96	1.16E-04	Sn-121m	7.83E-07	Pr-144m	3.62E-16
Nb-94	5.20E-09	Sn-122	2.51E-03	Nd-142	5.75E-06
Nb-95	3.60E-30	Sn-123	1.58E-16	Nd-143	1.93E-03
Nb-95m	2.18E-33	Sn-124	3.75E-03	Nd-144	1.67E-03
Mo-95	2.46E-03	Sn-126	8.67E-03	Nd-145	1.07E-03
Mo-96	9.98E-06	Sb-121	3.50E-04	Nd-146	8.67E-04
Mo-97	2.35E-03	Sb-123	4.42E-04	Nd-148	5.00E-04
Mo-98	2.42E-03	Sb-124	5.43E-34	Nd-150	2.01E-04
Mo-100	2.59E-03	Sb-125	1.56E-05	Pm-147	7.57E-06
Tc-99	4.40E-03	Sb-126	4.12E-10	Pm-148	4.11E-48
Ru-100	2.68E-05	Sb-126m	3.13E-12	Pm-148m	4.59E-46
Ru-101	2.06E-03	Te-122	3.08E-10	Sm-147	1.54E-03
Ru-102	2.28E-03	Te-124	1.43E-10	Sm-148	2.65E-04
Ru-103	1.28E-45	Te-125	4.32E-08	Sm-149	1.03E-04
Ru-104	1.15E-03	Te-125m	1.28E-11	Sm-150	1.47E-03
Sm-151	1.23E-04	U-232	8.33E-09	Pu-241	1.23E-04
Sm-152	5.28E-04	U-233	2.72E-09	Pu-242	3.74E-05
Sm-154	9.40E-05	U-234	1.43E-04	Am-241	7.48E-05
Eu-151	5.59E-06	U-235	7.35E-04	Am-242	2.05E-13

Table 9 Contd

Partial Isotopic Content of Feed from Salt Cell

Isotope	G/Gal	Isotope	G/Gal	Isotope	G/Gal
Eu-152	6.48E-08	U-236	1.76E-04	Am-242m	1.71E-08
Eu-153	1.09E-04	U-238	3.16E-01	Am-243	3.50E-07
Eu-154	5.43E-06	Np-236	1.59E-11	Cm-242	4.15E-11
Eu-155	1.27E-06	Np-237	1.53E-04	Cm-243	1.02E-09
Tb-159	8.31E-07	Pu-236	2.42E-10	Cm-244	1.59E-05
Tb-160	2.50E-31	Pu-237	7.13E-45	Cm-245	4.68E-10
Tl-206	9.15E-30	Pu-238	9.55E-04	Cm-246	2.09E-11
Tl-207	1.25E-20	Pu-239	2.49E-03	Cm-247	8.52E-14
Tl-208	9.72E-17	Pu-240	4.57E-04	Cm-248	1.95E-15
Tl-209	5.85E-25				

Total 1.56E+00 G/Gal

Table 10

Chemical Composition of Sludge-Precipitate Glass

Component	Water Free wt%
Ag	0.05
Al_2O_3	3.96
B_2O_3	10.28
$BaSO_4$	0.14
$Ca_3(PO_4)_2$	0.07
CaO	0.85
$CaSO_4$	0.08
Cr_2O_3	0.12
Cs_2O	0.08
CuO	0.19
Fe_2O_3	7.04
FeO	3.12
K_2O	3.58
Li_2O	3.16
MgO	1.36
MnO	2.00
Na_2O	11.00
Na_2SO_4	0.36
NaCl	0.19
NaF	0.07
NiO	0.93
PbS	0.07
SiO_2	45.57
ThO_2	0.21
TiO_2	0.99
U_3O_8	2.20
Zeolite	1.67
ZnO	0.08
Others	0.58
Total	100.00

Table 11

Radionuclide Content of Sludge-Precipitate Glass

Isotope	Ci/Lb	Isotope	Ci/Lb	Isotope	Ci/Lb
Cr-51	2.51E-20	Te-125m	7.44E-02	Eu-155	1.28E-01
Co-60	4.58E-02	Te-127	3.24E-05	Eu-156	1.41E-35
Ni-59	6.46E-06	Te-127m	3.31E-05	Tb-160	3.02E-10
Ni-63	8.02E-04	Te-129	8.23E-16	Tl-208	3.04E-07
Se-79	4.58E-05	Te-129m	1.28E-15	U-232	3.61E-06
Rb-87	2.35E-10	Cs-134	9.09E-02	U-233	4.27E-10
Sr-89	1.15E-08	Cs-135	2.68E-05	U-234	9.24E-06
Sr-90	1.26E+01	Cs-136	2.11E-43	U-235	4.24E-08
Y-90	1.29E+01	Cs-137	1.17E+01	U-236	3.04E-07
Y-91	2.04E-07	Ba-136m	2.32E-42	U-238	2.83E-06
Zr-93	3.01E-04	Ba-137m	1.12E+01	Np-236	4.70E-12
Zr-95	2.71E-06	Ba-140	2.76E-40	Np-237	2.40E-06
Nb-94	2.60E-08	La-140	1.16E-40	Pu-236	3.29E-05
Nb-95	5.70E-06	Ce-141	9.68E-15	Pu-237	2.41E-15
Nb-95m	3.36E-08	Ce-142	2.59E-09	Pu-238	4.00E-01
Tc-99	8.30E-04	Ce-144	2.66E+00	Pu-239	3.48E-03
Ru-103	4.54E-12	Pr-143	3.23E-38	Pu-240	2.34E-03
Ru-106	6.07E-01	Pr-144	2.66E+00	Pu-241	4.50E-01
Rh-103m	4.41E-12	Pr-144m	3.20E-02	Pu-242	3.30E-06
Rh-106	6.09E-01	Nd-144	1.31E-13	Am-241	2.97E-03
Pd-107	3.97E-06	Nd-147	3.40E-48	Am-242	3.87E-06
Ag-110m	3.39E-05	Pm-147	6.52E+00	Am-242m	3.90E-06
Cd-113	1.35E-17	Pm-148	1.88E-14	Am-243	1.56E-06
Cd-115m	3.27E-13	Pm-148m	2.72E-13	Cm-242	9.42E-06
Sn-121m	2.13E-05	Sm-147	5.39E-10	Cm-243	1.50E-06
Sn-123	6.87E-05	Sm-148	1.56E-15	Cm-244	2.90E-02
Sn-126	1.19E-04	Sm-149	4.80E-16	Cm-245	1.81E-09
Sb-124	1.92E-11	Sm-151	6.68E-02	Cm-246	1.44E-10
Sb-125	2.29E-01	Eu-152	9.94E-04	Cm-247	1.78E-16
Sb-126	1.66E-05	Eu-154	1.67E-01	Cm-248	1.85E-16
Sb-126m	1.19E-04				

Total activity	6.31E+01 Ci/Lb
Decay heat	
Total primary	1.42E-01 Watts/Lb
Total gammas	4.45E-02 Watts/Lb

Table 12

Partial Isotopic Content of Sludge-Precipitate Glass

Isotope	G/Lb	Isotope	G/Lb	Isotope	G/Lb
Cr-51	2.72E-25	Rh-103	2.34E-02	Te-126	3.62E-05
Co-60	4.05E-05	Rh-103m	1.35E-19	Te-127	1.23E-11
Ni-59	7.99E-05	Rh-106	1.71E-10	Te-127m	3.50E-09
Ni-63	1.36E-05	Pb-104	3.09E-03	Te-128	9.86E-03
Se-77	1.06E-04	Pd-105	2.46E-02	Te-129	3.93E-23
Se-78	2.67E-04	Pd-106	1.58E-02	Te-129m	4.25E-20
Se-79	6.57E-04	Pd-107	7.71E-03	Te-130	3.57E-02
Se-80	1.57E-03	Pd-108	4.55E-03	Cs-133	1.84E-01
Se-82	3.17E-03	Pd-110	1.58E-03	Cs-134	7.02E-05
Rb-85	1.09E-03	Ag-109	5.13E-03	Cs-135	2.33E-02
Rb-87	2.68E-03	Ag-110m	7.14E-09	Cs-136	2.85E-48
Sr-88	6.25E-02	Cd-110	1.79E-04	Cs-137	1.35E-01
Sr-89	3.96E-13	Cd-111	8.42E-04	Ba-134	8.93E-03
Sr-90	9.20E-02	Cd-112	5.81E-04	Ba-136	9.47E-04
Y-89	4.25E-02	Cd-113	3.97E-05	Ba-136m	8.56E-54
Y-90	2.37E-05	Cd-114	9.46E-04	Ba-137	3.63E-02
Y-91	8.31E-12	Cd-115m	1.28E-17	Ba-137m	2.08E-08
Zr-90	8.47E-03	Cd-116	4.37E-04	Ba-138	3.23E-01
Zr-91	6.72E-02	Sn-116	1.93E-04	Ba-140	3.79E-45
Zr-92	6.90E-02	Sn-117	9.98E-04	La-139	1.16E-01
Zr-93	1.20E-01	Sn-118	1.07E-03	La-140	2.09E-46
Zr-94	7.66E-02	Sn-119	1.04E-03	Ce-140	1.15E-01
Zr-95	1.26E-10	Sn-120	1.08E-03	Ce-141	3.40E-19
Zr-96	7.69E-02	Sn-121m	3.97E-07	Ce-142	1.08E-01
Nb-94	1.39E-07	Sn-122	1.21E-03	Ce-144	8.33E-04
Nb-95	1.46E-10	Sn-123	8.36E-09	Pr-141	1.07E-01
Nb-95m	8.84E-14	Sn-124	1.82E-03	Pr-143	4.80E-43
Mo-95	7.37E-02	Sn-125	4.82E-62	Pr-144	3.52E-03
Mo-96	2.99E-04	Sn-126	4.17E-03	Pr-144m	1.76E-10
Mo-97	7.05E-02	Sb-121	4.96E-04	Nd-142	3.81E-04
Mo-98	7.24E-02	Sb-123	6.28E-04	Nd-143	1.28E-01
Mo-100	7.73E-02	Sb-124	1.10E-15	Nd-144	1.10E-01
Tc-99	4.89E-02	Sb-125	2.22E-04	Nd-145	7.08E-02
Ru-100	5.16E-04	Sb-126	1.98E-10	Nd-146	5.75E-02
Ru-101	5.95E-02	Sb-126m	1.51E-12	Nd-147	4.23E-53
Ru-102	4.40E-02	Te-122	7.82E-06	Nd-148	3.33E-02
Ru-103	1.40E-16	Te-124	3.64E-06	Nd-150	1.33E-02
Ru-104	2.22E-02	Te-125	8.30E-04	Pm-147	7.03E-03
Ru-106	1.81E-04	Te-125m	4.13E-06	Pm-148	1.14E-19
Pm-148m	1.27E-17	Tl-206	9.01E-30	Pu-239	5.61E-02
Sm-147	2.32E-02	Tl-207	1.04E-19	Pu-240	1.03E-02
Sm-148	5.10E-03	Tl-208	1.03E-15	Pu-241	4.46E-03
Sm-149	2.00E-03	Tl-209	3.12E-24	Pu-242	8.42E-04

Table 12 Contd

Partial Isotopic Content of Sludge-Precipitate Glass

Isotope	G/Lb	Isotope	G/Lb	Isotope	G/Lb
Sm-150	2.83E-02	U-232	1.68E-07	Am-241	8.64E-04
Sm-151	2.54E-03	U-233	4.43E-08	Am-242	4.79E-12
Sm-152	1.02E-02	U-234	1.48E-03	Am-242m	4.01E-07
Sm-154	1.80E-03	U-235	1.96E-02	Am-243	7.85E-06
Eu-151	1.06E-04	U-236	4.70E-03	Cm-242	2.84E-09
Eu-152	5.63E-06	U-238	8.43E+00	Cm-243	2.91E-08
Eu-153	5.56E-03	Np-236	3.56E-10	Cm-244	3.59E-04
Eu-154	6.20E-04	Np-237	3.40E-03	Cm-245	1.05E-08
Eu-155	2.76E-04	Pu-236	6.19E-08	Cm-246	4.69E-10
Eu-156	2.57E-40	Pu-237	2.00E-19	Cm-247	1.91E-12
Tb-159	5.53E-05	Pu-238	2.34E-02	Cm-248	4.36E-14
Tb-160	2.68E-14				
	Total	1.15E+01 G/Lb			

Table 13A

Chemical Composition of Glass Frits, wt%

Oxide	Frit Number 18	21	131	165	200[a]	202
SiO_2	52.5	52.5	57.9	68.0	70.5	77.0
Na_2O	22.5	18.5	17.7	13.0	10.4	6.0
TiO_2	10.0	10.0	1.0	-	-	-
B_2O_3	10.0	10.0	14.7	10.0	12.1	8.0
Li_2O	-	4.0	5.7	7.0	5.0	7.0
MgO	-	-	2.0	1.0	2.0	2.0
ZrO2	-	-	0.5	1.0	-	-
La_2O_3	-	-	0.5	-	-	-
CaO	5.0	5.0	-	-	-	-

---

[a] Design basis frit.

Table 13B

Projected DWPF Waste Glass Compositions, wt %

Major Glass	Constituent Sludge Type						
Components	Blend	Batch 1	Batch 2	Batch 3	Batch 4	HM	Purex
Al_2O_3	3.98	4.87	4.46	3.25	3.32	7.08	2.89
B_2O_3	8.01	7.69	7.70	7.69	8.11	6.94	10.21
$BaSO_4$	0.27	0.22	0.24	0.26	0.38	0.18	0.29
CaO	0.97	1.17	1.00	0.93	0.83	1.00	1.02
$CaSO_4$	0.077	0.12	0.11	0.10	0.0034	trace	0.12
Cr_2O_3	0.12	0.10	0.12	0.13	0.14	0.086	0.14
CuO	0.44	0.40	0.41	0.40	0.46	0.25	0.42
Fe_2O_3	6.95	8.39	7.11	7.48	7.59	4.95	8.54
FeO	3.11	3.72	3.15	3.31	3.36	2.19	3.78
Group A[a]	0.14	0.099	0.14	0.10	0.20	0.20	0.078
Group B[b]	0.36	0.22	0.44	0.25	0.60	0.89	0.084
K_2O	3.86	3.49	3.50	3.47	3.99	2.14	3.58
Li_2O	4.40	4.42	4.42	4.42	4.32	4.62	3.12
MgO	1.35	1.36	1.35	1.35	1.38	1.45	1.33
MnO	2.03	2.06	1.62	1.81	3.08	2.07	1.99
Na_2O	8.73	8.62	8.61	8.51	8.88	8.17	12.14
Na_2SO_4	0.10	0.10	0.12	0.096	0.13	0.14	0.12
$NaCl$	0.19	0.31	0.23	0.22	0.090	0.093	0.26
NiO	0.89	0.75	0.90	1.07	1.09	0.40	1.21
SiO_2	50.20	49.81	50.17	49.98	49.29	54.39	44.56
ThO_2	0.19	0.36	0.63	0.77	0.24	0.55	0.011
TiO_2	0.90	0.66	0.67	0.66	1.02	0.55	0.65
U_3O_8	2.14	0.53	2.30	3.16	0.79	1.01	2.89

---

[a] Tc, Se, Te, Rb, and Mo.

[b] Ag, Cd, Cr, Pd, Tl, La, Ce, Pr, Pm, Nd, Sm, Tb, Sn, Sb, Co, Zr, Nb, Eu, Np, Am, and Cm.

Table 14

Physical Properties of Glass Wasteforms

Property	Value
Thermal conductivity at 100°C	0.55 Btu/(hr)(ft) (°F)
Heat capacity at 100°C	0.22 cal/(g)(°C) also Btu/(lb)(°F)
Fractional thermal expansion[a]	1.2 x 10-5/°C
Young's modulus	9 x 10^6 psi
Tensile strength	9 x 10^3 psi
Compressive strength	1 x 10^5 psi
Poisson's ratio	0.2
Density at 100°C[a]	2.75 ± 0.05 g/cc
Softening point	488°C

---

[a] Experimentally determined for Frit 131 and Frit 165 glasses containing composite waste.

Table 15

Composition of Simulated Wastes

Component	Simulation 1[a]	Simulation 2[a]	Simulation 3[a]
Fe_2O_3	47.3	13.8	59.1
MnO_2	13.6	11.3	4.0
Zeolite[b]	10.2	10.2	9.7
Al_2O_3	9.5	49.3	1.4
NiO	5.8	2.0	10.1
SiO_2	4.1	4.5	2.9
CaO	3.5	0.9	4.0
Na_2O	3.1	5.0	5.9
Coal	2.3	2.3	2.1
Na_2SO_4	0.6	0.7	0.8
Glassformer/ waste ratio	70.2/29.8	71.3/27.7	70.2/29.8

---

[a] Simulation 1 is composite waste; simulation 2 is high aluminum waste; simulation 3 is high iron waste.

[b] Zeolite composition is given in Table 18.

Table 16

Heat Capacities of Simulated Waste Glasses

	Heat Capacity - c_{pt} (cal/(g)(°C)			
Temperature, °C	Calculated Simulation 1 (Composite)	Measured Simulation 1 (Composite)	Measured Simulation 2 (High Al)	Measured Simulation 3 (High Fe)
0		0.186		
50	-	0.20	0.19	0.20
100	0.237	0.21	0.21	0.21
200	0.271	0.23	0.23	0.23
300	0.296	0.27	0.25	0.25
400	0.314			
500	0.328			
600	0.338			
700	0.346			
800	0.353			
900	0.359			
950	0.361			
1000	0.363			
1025	0.364			
1050	0.365			
1075	0.366			
1100	0.367			
1125	0.368			
1150	0.369			
1175	0.369			
1200	0.370			
1250	0.372			
1300	0.373			

Table 17

Measured Density of SRP Simulated Waste Glasses

Glass	Density, g/cm3 at 25°C
Simulation 1 - composite	2.75
Simulation 2 - high Al	2.56
Simulation 3 - high Fe	2.92

Table 18

Zeolite Composition

Component	Wt %
SiO_2	48.0
H_2O	19.1
Al_2O_3	18.6
CaO	10.2
Na_2O	4.1

Table 19

Canister Decay Heat and Activity

Year	Design Basis Curies/Can	Waste Glass Watts/Can
5	233,000	690
10	171,000	517
20	129,000	406
30	101,000	324
40	80,000	262
50	63,000	211
60	50,000	171
70	39,000	139
80	31,000	115
90	25,000	94
100	20,000	78
200	2,200	17
300	400	7.2
400	160	4.1
500	96	2.7
600	70	2.0
700	57	1.6
800	49	1.4
900	45	1.2
1000	41	1.1

Table 20

Radiation from Canister of Sludge-Precipitate Glass

Distance, ft	Gamma, R/hr	Neutron, Mead hr	Total, rad hr
Surface	5570	420	5570
1	2190	97	2190
3	900	42	900
5	470	23	470
10	160	7.5	160
20	44	2.5	44
30	20	1.0	20
50	7	0.5	7
75	3	-	3
100	2	-	2

Table 21

Chemical Composition of Sludge-Precipitate Glass for Radiation Calculations[a]

Component	Wt %	Component	Wt %
Al_2O_3	3.96	MnO	2.00
B_2O_3	10.28	SiO_2	46.72
CaO	0.85	TiO_2	0.99
Fe_2O_3	7.04	U_3O_8	2.20
FeO	3.12	Na_2O	12.15
K_2O	3.58	NiO	0.93
Li_2O	3.16	Zeolite[b]	1.67
MgO	1.36		

---

[a] Compounds present at >0.8 wt %.

[b] Zeolite composition is given in Table 18.

Table 22

Source Terms for Sludge-Precipitate Glass for Radiation Calculations

		Neutron Yields, n/s/canister	
Isotope	Ci/Lb	a.n	SF
U-232	3.61E-06	6.811E-02	1.207E-03
U-233	4.27E-10	8.056E-02	2.104E-07
U-234	9.24E-06	1.743E+03	4.196E-02
U-235	4.24E-08	7.999	3.167E-02
U-236	3.04E-07	5.735E+01	1.321E-01
U-238	2.83E-06	5.339E+02	5.764E+02
Np-237	2.40E-06	4.528E+02	1.908E-03
Pu-236	3.29E-05	6.207E+03	9.927
Pu-233	4.00E-01	7.547E+07	2.732E+05
Pu-239	3.48E-03	6.565E+05	5.672
Pu-240	2.34E-03	4.415E+05	4.346E+04
Pu-241	4.50E-01	2.080E+03	0.0
Pu-242	3.30E-06	6.226E+02	6.834E+03
Am-241	2.97E-03	5.603E+05	4.156
Am-242	3.87E-06	0.0	0.0
Am-242m	3.90E-06	3.502	2.307E-01
Am-243	1.56E-06	2.943E+02	2.136E-02
Cm-242	9.42E-06	1.777E+03	2.563E+02
Cm-243	1.50E-06	2.830E+02	1.588E-08
Cm-244	2.90E-02	5.471E+06	1.450E+07
Cm-245	1.81E-09	3.415E-01	5.740E-09
Cm-246	1.44E-10	2.717E-02	1.400E+01
Cm-247	1.78E-16	3.358E-08	1.032E-12
Cm-248	1.85E-16	3.490E-08	5.075E-03

Table 23

Major Contributing Isotopes to Gamma Dose Rates

Isotope	R/hr at 10 ft	Percent of Dose
Cs/Ba-137	142.1	88.8
Eu-154	4.9	3.1
Co-60	3.1	2.0
Cs-134	3.1	1.9
Ce/Pr-144	2.6	1.6
Ru/Rh-106	2.3	1.4
Sb-125	1.8	1.1
Others	0.1	0.1
Total	160.0	100.0

Table 24

Gamma Radiation from Canister - Comparison of Calculations

Distance From Canister Surface, ft	SRP (ANISN/QAD-CG)[a] R/hr	GA (PATH)[a] R/hr	Westinghouse (SCAP/ANISN-W),[a] R/hr	Bechtel (GRACE-II),[a] R/hr
0	5,570	7,600	11,300	10,970
1	2,190	3,500	4,500	4,760
2	1,500	2,180		2,885
3	900	1,500	1,860	1,920
4	690	1,070		1,350
5	470			990
7	350	490		590
10	160	270		320
15	75	130		155
20	44			89
30	20	34		40
50	7	12		14
75	3	5		6
100	2	3		3

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[a] Calculational code used.

Table 25

Neutron Radiation from Canister - Comparison of Calculations

Distance from Canister Surface, ft	SRP (ANISN),[a] mrem/hr	GA (DTF)[a] mrem/hr	Westinghouse (ORIGEN/SOURCES/ANISN/WEST),[a] mrem/hr
0	420	250	305
1	97		
3	42		
5	23		
10	7.5		
20	2.5		
30	1.0		
50	0.5		

---

[a] Calculational code used.

Table 26

Reference Canister Temperatures[a]

Watts	Surface Temp. °C	Centerline Temp. °C	Surrounding Air Temp. °C
425	34	50	20
510	54	71	38
1000	66	120	38

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[a] Reference DWPF sludge-precipitate waste form: canister 24-in. OD by 118 in. high, 22 ft^3 of waste glass containing 28% sludge oxides, and air convection cooling.

Table 27

Projected Waste Inventory and Fission Product Radioactivity as of December 31, 1988

	Volume, (10^3) m^3	Activity (10^6) Ci	Power (10^3) W
Sludge	13.9	495.5	1546.1
Salt Cake	50.4	193.7	501.0
Liquid	62.1	88.8	245.3
Precipitate	0.2	1.3	3.3
Total	126.6	779.3	2295.7

NOTE: 1 m^3 = 264.2 gal.

Isotope	Curies
Sr-89	1,219,000
Sr-90	137,200,000
Y-90[a]	137,300,000
Y-91	2,633,000
Zr-95	4,805,000
Nb-95[a]	10,410,000
Ru-106	5,583,000
Rh-106[a]	5,583,000
Cs-137	141,500,000
Ba-137[a]	130,200,000
Ce-144	70,630,000
Pr-144[a]	70,640,000
Pm-147	60,220,000
Total	777,923,000

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[a] Daughter isotopes.

Table 28

Estimated Production Schedule and Estimated Cumulative Average Radioactivity and Thermal Power per Canister of HLW Glass

End of Calendar Year	No. of Canisters Produced During Year	Cumulative No. of Canisters Produced	Cumulative Radioactivity		Cumulative Thermal Power	
			Total, $\text{Ci}(10^6)$	Per Canister, Ci	Total, $\text{W}(10^3)$	Per Canister, W
1991	0	0	--	--	--	--
92	102	102	--	--	--	--
93	410	512	32.9	64,260	87.3	171
94	410	922	52.9	57,380	142.7	155
95	410	1,332	70.0	52,550	190.9	143
96	410	1,742	81.7	46,900	223.5	128
97	410	2,152	91.0	42,290	249.1	116
98	410	2,562	100.5	39,230	275.0	107
99	376	2,938	110.3	37,540	302.1	103
2000	205	3,143	120.1	38,210	329.0	105
01	205	3,348	135.0	40,320	371.8	111
02	205	3,553	158.8	44,690	440.7	124
03	205	3,758	185.0	49,230	516.3	137
04	205	3,963	205.2	51,780	572.9	145
05	205	4,168	218.4	52,400	609.5	146
06	205	4,373	233.8	53,460	653.2	149
07	205	4,578	248.2	54,220	694.6	152
08	205	4,783	264.3	55,260	740.9	155
09	205	4,988	276.1	55,350	774.5	155
2010	205	5,193	283.9	54,670	798.0	154
11	161	5,354	296.4	55,360	835.5	156
12	30	5,384	306.4	56,910	864.9	161
13	31	5,415	313.1	57,820	884.3	163
14	30	5,445	317.5	58,310	896.4	165
15	31	5,476	370.5	58,530	904.4	165
16	30	5,506	323.0	58,660	910.8	165
17	31	5,537	326.0	58,880	918.7	166
18	30	5,567	329.4	59,170	929.2	167
19	31	5,598	336.0	60,020	949.6	170
2020	30	5,628	343.5	61,030	971.5	173
21	31	5,659	348.6	61,600	986.0	174
22	30	5,689	347.5	61,080	982.7	173

Calculated from estimates provided for 1988 Integrated Data Base. Year-by-year radioactivity and thermal power per canister do not necessarily represent actual processing schedules and tankage allocations. Radioactivity and thermal power shown are for fission products only. Radioactivity will be about 1% higher and thermal power about 6% higher when actinides are included.

WVMP SAR Reference 3-17

Significant of Tests and Properties of Concrete and Concrete-making Materials, ASTM Report STP 169D, J. F. Lamond and J. H. Pielert, ASTM International, West Conshohocken, Pennsylvania, 2006.

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The mixing technique should be compatible with the type of mixture, its ingredients, the job requirements, and the method of placement. Mixing should efficiently mix the cement and water and properly blend them with the other ingredients including the preformed foam. Paddle, high shear, continuous, and rotary drum mixers may all be acceptable for specific applications depending on the quality requirements of the final product.

Cellular concrete mixtures are typically job-site produced and placed. If ready-mix trucks are used for sanded mixtures (floor-fill applications), the grout is delivered to the job site and the preformed foam is added just prior to placement. This maintains the quality and freshness of the material. The rotary drum action of a ready-mix truck is acceptable for sanded mixtures with densities greater than 800 kg/m^3 (50 lb/ft^3).

For low-density applications (roof deck and engineered fill) with neat-cement slurries at densities less than 800 kg/m^3 (50 lb/ft^3), rotary drum mixing action is not ideal. Instead, paddle type or shear mixers are common methods for both batch and continuous mixing procedures. After the cement/water slurry is produced in these mixers, the preformed foam is added and blended prior to or during placement with a positive displacement pump. As the mixture is pumped, density is measured at the point of placement for quality control. Mix adjustments can then be made to account for pumping distances and other special application conditions.

Pumping is the most common method of placement, but other methods can be used. Positive displacement pumps such as Moyno or peristaltic pumps are used for low-density mixtures. Although piston pumps are efficient for grout mixtures at densities greater than 1440 kg/m^3 (90 lb/ft^3), they do not efficiently pump low-density mixtures.

Casting techniques are different for each type of cellular concrete application. The thinner floor-fill mixtures utilize a rolling screed to provide a constant thickness. Since roof deck applications are cast slope-to-drain, string lines provide guides for casting and darby finishing the material by experienced tradesmen. Screed rails may also be used. Finishing operations, in general, should be kept to a minimum; smoothing with a darby or bullfloat is usually sufficient [20].

Geotechnical fill applications have the greatest variation in casting techniques. Generally, these fills are several meters (feet) thick so that they are cast in lifts of up to 1.0 m (3.3 ft) thick based on the available area to be cast. The succeeding lifts are cast on a daily basis until the final fill profile is reached.

Physical Properties

Several studies investigated the physical and mechanical properties of cellular concrete cast at different densities and with or without aggregates in the mix [1,18–21]. Because the density of cellular concrete may be varied over a wide range, 320–1920 kg/m^3 (20–120 lb/ft^3), it is considered as an additional variable that significantly impacts the physical properties and the mix design of the material. The lower density cellular concrete has lower thermal conductivity (higher insulation), accompanied by lighter weight and reduced strength. As the density

Density

When referring to the density of cellular concrete, confusion may be avoided by stating the moisture condition of the material at that specific density. Significant moisture conditions include as-cast density (wet density or plastic concrete density), air-dry density (at a stated age and curing condition), and the oven-dry density.

The as-cast or wet density is usually determined at the point of placement in accordance with ASTM C 796. In determining the wet density, the concrete should be consolidated by tapping the sides of the container and not by rodding. The ratio of the wet density to oven-dry density for the different cellular concrete mixtures varies due to the different water content requirements. The wet density of the cellular concrete is an important job-site quality assurance tool to control uniformity of the mixtures.

The air-dry density of cellular concrete usually represents the condition of the in-place material. The change in density due to air drying is a function of temperature, duration of the drying period, humidity, the wet density of the concrete, the water-cement ratio, and the surface-area ratio of the element. Although the relationship between air-dry density and wet density seems complicated, the air-dry density of cellular concrete is usually about 80 kg/m^3 (5 lb/ft^3) less than its wet density. Cellular concrete cast, cured, and air dried under job conditions in low-humidity environments may have density losses approaching 160 kg/m^3 (10 lb/ft^3).

Oven-dry density is commonly used to relate the physical properties of various types of cellular concretes, and for the determination of the thermal conductivity by the guarded hot plate method in accordance with ASTM Standard Test Method for Steady-State Heat Flux Measurements and Thermal Transmission Properties by Means of the Guarded-Hot-Plate Apparatus (C 177). For the latter purpose, the oven-dry density may be calculated with sufficient accuracy from the mixture data by assuming that the water required for hydration of the cement is 20 % of the weight of the cement. The oven-dry density (D) is calculated as follows:

$$D = [1.2\,C + A]\ \mathrm{kg/m^3}\ \text{or}\ [(1.2\,C + A)/27]\ \mathrm{lb/ft^3}$$

where

C = weight of cement, kg/m^3 (lb/yd^3) of concrete; and
A = weight of aggregate, kg/m^3 (lb/yd^3) of concrete.

Workability

Cellular concrete in the low-density range (less than 800 kg/m^3 (50 lb/ft^3)) is a flowable material with excellent workability. As a result, it is handled as a liquid and poured or pumped into place without the need for consolidation. It should be pointed out that the slump test, which is used to measure the consistency in normal weight concrete, is meaningless in the case of cellular concrete since the material is placed in fluid consistency.

Thermal Conductivity

The thermal conductivity (k) of a material is the time rate of transfer of heat by conduction, through a unit thickness, across a unit area for a unit difference of temperature. The units of k

J. F. Lamond and J. H. Pielert, Significant of Tests and Properties of Concrete and Concrete-making Materials, ASTM Report STP 169D, ASTM International, West Conshohocken, Pennsylvania (2006), p. 564.

WVMP SAR Reference 3-18

ACI Manual of Concrete Inspection, 10th ed., American
Concrete Institute Committee 311 Report SP-2(07),
American Concrete Institute, 2008.

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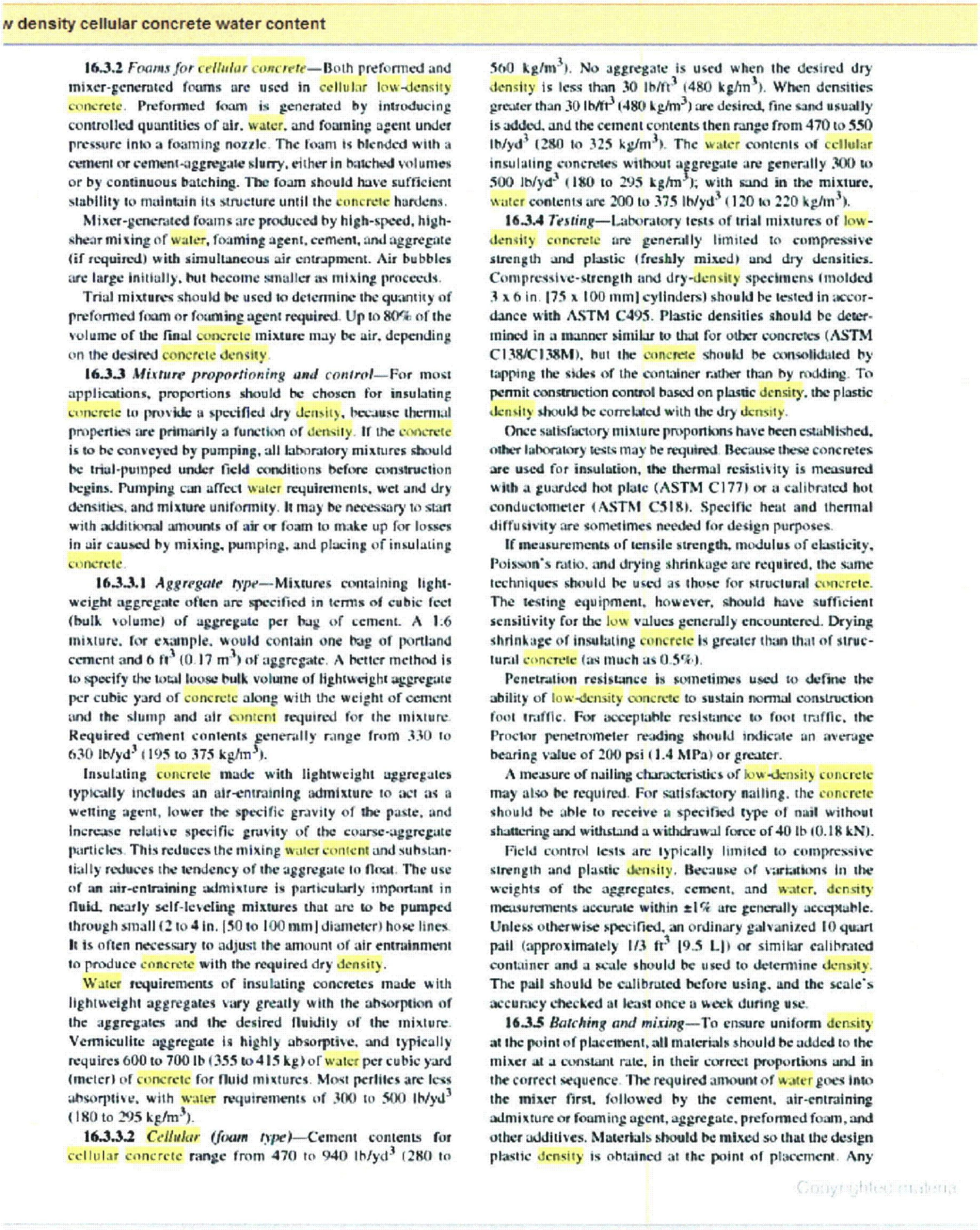

16.3.2 *Foams for cellular concrete*—Both preformed and mixer-generated foams are used in cellular low-density concrete. Preformed foam is generated by introducing controlled quantities of air, water, and foaming agent under pressure into a foaming nozzle. The foam is blended with a cement or cement-aggregate slurry, either in batched volumes or by continuous batching. The foam should have sufficient stability to maintain its structure until the concrete hardens.

Mixer-generated foams are produced by high-speed, high-shear mixing of water, foaming agent, cement, and aggregate (if required) with simultaneous air entrapment. Air bubbles are large initially, but become smaller as mixing proceeds.

Trial mixtures should be used to determine the quantity of preformed foam or foaming agent required. Up to 80% of the volume of the final concrete mixture may be air, depending on the desired concrete density.

16.3.3 *Mixture proportioning and control*—For most applications, proportions should be chosen for insulating concrete to provide a specified dry density, because thermal properties are primarily a function of density. If the concrete is to be conveyed by pumping, all laboratory mixtures should be trial-pumped under field conditions before construction begins. Pumping can affect water requirements, wet and dry densities, and mixture uniformity. It may be necessary to start with additional amounts of air or foam to make up for losses in air caused by mixing, pumping, and placing of insulating concrete.

16.3.3.1 *Aggregate type*—Mixtures containing lightweight aggregate often are specified in terms of cubic feet (bulk volume) of aggregate per bag of cement. A 1:6 mixture, for example, would contain one bag of portland cement and 6 ft^3 (0.17 m^3) of aggregate. A better method is to specify the total loose bulk volume of lightweight aggregate per cubic yard of concrete along with the weight of cement and the slump and air content required for the mixture. Required cement contents generally range from 330 to 630 lb/yd^3 (195 to 375 kg/m^3).

Insulating concrete made with lightweight aggregates typically includes an air-entraining admixture to act as a wetting agent, lower the specific gravity of the paste, and increase relative specific gravity of the coarse-aggregate particles. This reduces the mixing water content and substantially reduces the tendency of the aggregate to float. The use of an air-entraining admixture is particularly important in fluid, nearly self-leveling mixtures that are to be pumped through small (2 to 4 in. [50 to 100 mm] diameter) hose lines. It is often necessary to adjust the amount of air entrainment to produce concrete with the required dry density.

Water requirements of insulating concretes made with lightweight aggregates vary greatly with the absorption of the aggregates and the desired fluidity of the mixture. Vermiculite aggregate is highly absorptive, and typically requires 600 to 700 lb (355 to 415 kg) of water per cubic yard (meter) of concrete for fluid mixtures. Most perlites are less absorptive, with water requirements of 300 to 500 lb/yd^3 (180 to 295 kg/m^3).

16.3.3.2 *Cellular (foam type)*—Cement contents for cellular concrete range from 470 to 940 lb/yd^3 (280 to 560 kg/m^3). No aggregate is used when the desired dry density is less than 30 lb/ft^3 (480 kg/m^3). When densities greater than 30 lb/ft^3 (480 kg/m^3) are desired, fine sand usually is added, and the cement contents then range from 470 to 550 lb/yd^3 (280 to 325 kg/m^3). The water contents of cellular insulating concretes without aggregate are generally 300 to 500 lb/yd^3 (180 to 295 kg/m^3); with sand in the mixture, water contents are 200 to 375 lb/yd^3 (120 to 220 kg/m^3).

16.3.4 *Testing*—Laboratory tests of trial mixtures of low-density concrete are generally limited to compressive strength and plastic (freshly mixed) and dry densities. Compressive-strength and dry-density specimens (molded 3 x 6 in. [75 x 100 mm] cylinders) should be tested in accordance with ASTM C495. Plastic densities should be determined in a manner similar to that for other concretes (ASTM C138/C138M), but the concrete should be consolidated by tapping the sides of the container rather than by rodding. To permit construction control based on plastic density, the plastic density should be correlated with the dry density.

Once satisfactory mixture proportions have been established, other laboratory tests may be required. Because these concretes are used for insulation, the thermal resistivity is measured with a guarded hot plate (ASTM C177) or a calibrated hot conductometer (ASTM C518). Specific heat and thermal diffusivity are sometimes needed for design purposes.

If measurements of tensile strength, modulus of elasticity, Poisson's ratio, and drying shrinkage are required, the same techniques should be used as those for structural concrete. The testing equipment, however, should have sufficient sensitivity for the low values generally encountered. Drying shrinkage of insulating concrete is greater than that of structural concrete (as much as 0.5%).

Penetration resistance is sometimes used to define the ability of low-density concrete to sustain normal construction foot traffic. For acceptable resistance to foot traffic, the Proctor penetrometer reading should indicate an average bearing value of 200 psi (1.4 MPa) or greater.

A measure of nailing characteristics of low-density concrete may also be required. For satisfactory nailing, the concrete should be able to receive a specified type of nail without shattering and withstand a withdrawal force of 40 lb (0.18 kN).

Field control tests are typically limited to compressive strength and plastic density. Because of variations in the weights of the aggregates, cement, and water, density measurements accurate within ±1% are generally acceptable. Unless otherwise specified, an ordinary galvanized 10 quart pail (approximately 1/3 ft^3 [9.5 L]) or similar calibrated container and a scale should be used to determine density. The pail should be calibrated before using, and the scale's accuracy checked at least once a week during use.

16.3.5 *Batching and mixing*—To ensure uniform density at the point of placement, all materials should be added to the mixer at a constant rate, in their correct proportions and in the correct sequence. The required amount of water goes into the mixer first, followed by the cement, air-entraining admixture or foaming agent, aggregate, preformed foam, and other additives. Materials should be mixed so that the design plastic density is obtained at the point of placement. Any

ACI Manual of Concrete Inspection, 10th ed., American Concrete Institute Committee 311 Report SP-2(07), American Concrete Institute (2008), p. 126.