USE OF GEOMETRICALLY-ACCURATE MODELS TO PREDICT SPENT NUCLEAR FUEL CLADDING TEMPERATURES WITHIN A TRUCK CASK UNDER NORMAL AND FIRE ACCIDENT CONDITIONS

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ABSTRACT

The temperature of spent nuclear fuel cladding within transport casks must be determined for both normal conditions of transport and hypothetical fire accident conditions to assure that it does not exceed certain limit conditions. In the current work a two-dimensional finite-element thermal model of a legal-weight truck cask is constructed that accurately models the geometry of the fuel rods and cover gas. Computational fluid dynamics (CFD) simulations are performed that include buoyancy induced motion in, and radiation and natural convection heat transfer across the cover gas, as well as conduction in all solid components. Separate simulations are performed using helium or nitrogen cover gas. Stagnant-gas CFD (SCFD) simulations are preformed and compared to CFD simulations to determine the effect of gas motion.

For normal conditions of transport, the peak clad temperature is determined for a range of fuel heat generation rates to determine the thermal dissipation capacity based on peak cladding and surface temperature, Q_c and Q_s. These are respectively, the fuel heat generation rates that bring the peak cladding temperature to 400°C, or the peak surface temperature to 85°C (their allowed limits for normal transport). Transient fire/post fire simulations are then performed for a range of fire durations to determine the critical durations for cladding Creep Deformation or Burst Rupture, D_{CD} or D_{BR}. These are the fire durations that bring the cladding temperature to 570°C or 750°C, respectively.

When the cladding temperature is used to select the fuel heat generation rate, the thermal dissipation capacity is 3265 W/assembly when helium is the cover gas, which is 30% higher when nitrogen is used (due to helium’s higher thermal conductivity). When nitrogen is the cover gas, the critical fire durations for creep deformation and burst rupture are, respectively, 3.3 and 7.2 hours. These durations are 18% and 14% shorter for helium (because the allowed fuel heat generation rate is higher for helium). When the fuel heat generation is chosen based on the package surface temperature, for helium, the thermal dissipation capacity is 1040 W/assembly, and the critical fire durations for creep deformation and burst rupture are, respectively, 4.7 and 11.6 hours. The values for nitrogen are all within 4% of these values. The CFD and SCFD simulations give essentially the same results. This indicates that gas motion does not significantly affect the cladding temperature, and the future calculations may not need to incur the increased computation expense required to model that motion.
INTRODUCTION

The goal of this work is to develop computational tools to predict the temperature of spent nuclear fuel (SNF) cladding in transport casks during normal transport and severe fire events. Light water reactor nuclear fuel assemblies consist of fuel rods held in square arrays by periodic spacer plates [U.S. Department of Energy 1987; Bahney and Lotz 1996; Office of Civilian Radioactive Waste management 1987]. The rods themselves are stacks of UO$_2$ fuel pellets within zircaloy cladding. Some spaces in the array contain hollow instrumentation or guide thimble tubes instead of fuel rods, and some assemblies have zircaloy channels surrounding the rods.

Spent nuclear fuel is stored under-water after it is removed from a reactor to allow its heat generation and radioactive decay rates to decrease [Saling and Fentiman 2001]. The fuel can then be placed in dry casks for storage or offsite transport. In transport casks, individual SNF assemblies are supported horizontally within square cross-section compartments of a basket structure inside the cask’s containment region. That region is evacuated and backfilled with Helium (He), Nitrogen (N$_2$) or another non-oxidizing cover gas. Casks transported by truck have enough space for roughly four pressurized water reactor (PWR) assemblies, while those transported by rail hold around 21 assemblies [General Atomics 1998; Office of Civilian Radioactive Waste Management 1993]. Spent fuel cannot be loaded into casks until (a) its heat generation rate is low enough so that cask and fuel assembly components will not exceed their long-term limit temperatures, (b) its radioactive decay is low enough so that the cask can provide shielding and criticality control, and (c) other operational requirements are met. In this work we are concerned with maintaining component temperatures below their limits during both normal and fire accident conditions.

The zircaloy cladding that encapsulates the UO$_2$ pellets provides an important containment boundary. However, it may develop radial hydrides and become brittle if its temperature exceeds 400°C for extended periods [Johnson and Gilbert 1983]. The clad temperature must therefore be below 400°C during normal conditions of transport [U.S. Nuclear Regulatory Commission 2005]. Federal regulations also require that cask surface temperature be below 85°C when it is in the shade [U.S. Nuclear Regulatory Commission 2002] (this requirement may be avoided if a personnel barrier is used). Fuel cladding may experience creep deformation or burst rupture if its temperature exceeds 570°C or 750°C, respectively [Johnson and Gilbert 1983; Sprung et al. 2000]. It is therefore important that these temperatures be avoided even under fire accident conditions.

Cask operators must determine cask thermal dissipation capacity, $Q_{TDC}$. These are the fuel heat generation rates that cause either the peak fuel cladding temperature to reach 400°C ($Q_C$) during normal transport, or the surface temperature to reach 85°C ($Q_S$) in the shade. These capacities help operators determine how long the fuel must be aged underwater before it can be loaded into a dry cask. Transportation risk analysts also need to determine how long a transport cask can be in a fire before it reaches either 570°C or 750°C. These critical fire durations $D_F$ helps analysts determine which fires have a potential to challenge the integrity of the cladding.

Thermal analysis is used to determine thermal dissipation capacities and critical fire durations. This analysis typically involves construction of finite element or finite difference models of intact or damaged packages. First the steady state package temperatures are calculated for a normal transport environment [U.S. Nuclear Regulatory Commission 2002]. These temperatures are used as initial conditions for a transient calculation that determine the time-dependent package temperatures during a fire. Finally, the package temperatures at the end of the fire are used as initial conditions for a post-fire cool down calculation.

The multiple fuel regions within a cask are difficult to model because each contains many fuel rods. In the past, computational resources were not available to perform calculations using models that accurately represented the fuel geometry. To address this problem, the fuel assemblies and cover gas were replaced by homogenized models. These employ fictitious solid elements with temperature-dependent Effective Thermal Conductivities (ETC) [Bahney and Lotz 1996; General Atomics 1998; Unterzuber et al. 1982; Manteufel and Todreas 1994].

A shortcoming of homogenized fuel region models with effective conductivities is that they calculate heat flux at a location based only on the temperature and its spatial gradient at that location. This is not universally appropriate when radiation heat transfer is significant since radiant heat fluxes depend on temperatures at a distance. Natural convection heat flux is also dependent on local velocity, which depends on temperatures at different locations. As a result, an effective conductivity model that is developed for a fuel compartment with isothermal walls [Bahney and Lotz 1996] may not be accurate for compartments with highly non-uniform temperature surfaces.

Homogenized fuel region models have been used to predict peak fuel cladding temperatures under normal conditions of transport in safety analysis reports used to license transport casks [General Atomics 1996]. They have also been used to estimate the thermal dissipation capacity of rail [Greiner et al. 2006] and truck [Venigalla and Greiner 2007] transport casks. These works show that the surface temperature of basket compartments near the periphery of casks is highly non-uniform. As mentioned earlier, effective thermal conductivity models may not be accurate under those circumstances.

Homogenized fuel region models with effective conductivities have also been used to predict the peak cladding temperature during fire accident conditions [Greiner et al. 1998a; Greiner et al. 1998b; Greiner et al. 2006; and Adkins et al. 2006]. However, to our knowledge, the accuracy of this approach for the high temperature and transient conditions that exist during and after a fire has not been evaluated.

In recent years, computational resources needed for geometrically-accurate fuel region models (distinct fuel rods...
and gas regions as opposed to a homogenized model) have become readily available. Venigalla and Greiner [2007] performed two-dimensional thermal simulations of a truck cask designed to transport four PWR assemblies. Gudipati and Greiner [2007] analyzed a much larger rail cask designed for 21 PWR assemblies. Both studies employed meshes that accurately modeled the geometry of 15x15 arrays of fuel rods within all the fuel support compartments and considered both Helium (He) and Nitrogen (N\textsubscript{2}) cover gases. These meshes were used in two types of calculations. In the first computational fluid dynamics (CFD) simulations calculated buoyancy-induced gas motion within, and natural convection and radiation heat transfer across, the gas filled regions. In the second, stagnant-gas CFD (SCFD) simulations, with zero gas speed, were compared to the CFD results to evaluate the effect of gas motion. The thermal dissipation capacities for casks that used He as the cover gas were significantly larger than those for that used N\textsubscript{2} due to He’s higher thermal conductivity. The values of Q\textsubscript{TDC} predicted by CFD and SCFD simulations were very close, indicating that gas motion does not significantly affect the peak fuel cladding temperatures in these casks.

In the current work, we reconsider the geometrically accurate truck cask model developed by Venigalla and Greiner [2007] but include an improved external surface natural convection model. First, the fuel thermal dissipation capacities are determined that bring the peak fuel clad temperature to 400 °C during normal conditions of transport, or the cask surface temperature to 85 °C, for both He or N\textsubscript{2} cover gases. Fire and post fire simulations are then performed using these fuel heat generation rates as initial conditions. These simulations are performed with different fire durations to determine the minimum durations that brings the fuel cladding to 570 °C or 750 °C after the fire is extinguished. To our knowledge, this is the first time a geometrically accurate cask model has been used in a systematic fashion to determine cladding temperatures caused by a fire.

**NOMENCLATURE**

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>BWR</td>
<td>Boiling Water Reactor</td>
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<td>CFD</td>
<td>Computational Fluid Dynamics</td>
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<td>ETC</td>
<td>Effective Thermal Conductivity</td>
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<td>LWT</td>
<td>Legal Weight Truck</td>
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<td>PWR</td>
<td>Pressurized Water Reactor</td>
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<td>SNF</td>
<td>Spent Nuclear Fuel</td>
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<td>Q</td>
<td>Heat Generation Rate within an individual fuel assembly</td>
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<td>QC</td>
<td>Maximum allowable heat generation rate at which the fuel clad reaches its limit temperature</td>
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<tr>
<td>QS</td>
<td>Maximum allowable heat generation rate at which the package surface reaches its limit temperature</td>
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<td>QTDC</td>
<td>Fuel thermal dissipation capacity</td>
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<tr>
<td>T\textsubscript{AVG}</td>
<td>Average of the temperature along the walls of fuel basket</td>
</tr>
<tr>
<td>ΔT\textsubscript{MAX}</td>
<td>Maximum Temperature difference in a basket</td>
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<tr>
<td>T\textsubscript{CLAD, MAX}</td>
<td>Temperature of cladding concern</td>
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<tr>
<td>T\textsubscript{SURF, MAX}</td>
<td>Temperature of surface concern</td>
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<td>T\textsubscript{ENV}</td>
<td>Environmental Temperature</td>
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<td>Support Structure surface emissivity</td>
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<td>Accurate Geometry</td>
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<td>HG</td>
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<td>D\textsubscript{CD}</td>
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<tr>
<td>D\textsubscript{BR}</td>
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<td>Peak Surface Temperature</td>
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<td>T\textsubscript{SURFACE}</td>
<td>Surface Temperature</td>
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<td>Prandtl Number</td>
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<tr>
<td>Ra</td>
<td>Rayleigh Number</td>
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<td>Nu</td>
<td>Nusselt Number</td>
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<td>v</td>
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<td>α</td>
<td>Gas Thermal Diffusivity</td>
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**COMPUTATIONAL MODEL**

Figure 1 shows the cross section of a Legal Weight Truck (LWT) package modeled in the current analysis. It is similar but not identical to the currently licensed cask [General Atomics 1996]. The cross section in Fig. 1 is midway between cask ends. The dot-filled regions represent four 15x15 Pressurized Water Reactor (PWR) fuel assemblies within cover gas. All 225 fuel rods of each assembly are identical. Each contains UO\textsubscript{2} pellets of diameter 9.36 mm enclosed in zircaloy cladding of thickness 0.78 mm. No gap or contact resistance between the pellets and cladding are modeled. The tube array has center to center spacing of 14.5 mm, and the distance between the center of the outer most rod and the basket wall is 9.82 mm. The fuel assembly is similar to a Babcock & Wilcox 15x15 Mark B PWR [Bahney and Lotz 1996], but the model does not contain unheated components.

The cross-shaped component at the center of the package is a 1.5 cm thick stainless steel support structure. Its surface emissivity is 0.8. Borated Carbon (B\textsubscript{4}C) pellets fill 1.1 cm-diameter holes that are drilled radially in the legs of the structure. The sides of the four square compartments where the fuel is placed are 22.3 cm long.
The support structure and fuel are surrounded by a 0.96 cm thick stainless steel liner. Its emissivity is 0.2. The liner is surrounded by a depleted uranium gamma shield. Its maximum thickness is 6.7 cm and it has an outer radius of curvature of 11.4 cm at its corners. A 3.8 cm thick stainless steel package body surrounds the gamma shield.

An external, 12.4-cm-thick neutron shield encircles the package. A single region is used to model several components. These components are 12.1-cm-thick Polypropylene-1% boron, 24 aluminum radial fins of thickness 0.245 cm, and a 0.27 cm thick stainless steel outer skin. The density and specific heat capacity used for Neutron Shield are 2691 kg/m³ and 927 J/kg-K, respectively. The mixture thermal conductivity for this composite structure was developed based on an equivalent conduction model [Mallidi et al. 2006].

Figure 2 shows the computational mesh used in the current work. Only one half of the cask cross section is modeled to take advantage of the geometric and boundary condition symmetry. The computational grid was constructed using MSC Patran software. The dimensions are the same as those described for Fig 1. Mesh independence is evaluated using coarse and fine grids with 46,798 and 210,874 elements, respectively. Portions of the fuel region of those meshes are shown in Figs 2b and 2c. They show grid points within the fuel pellet, cladding and the cover-gas region. The majority of simulations presented in this work use the coarse grid.

The meshes in Fig. 2 are imported into the Fluent CFD package. That code utilizes the finite volume method to solve the governing mass, momentum and energy equations. A second order upwind discretization scheme is used to solve the momentum and energy equations along with SIMPLEC algorithm for pressure velocity coupling. For the CFD simulations, Fluent calculates buoyancy induced gas motion, convection and surface-to-surface radiation heat transfer across the gas filled regions, and conduction in the solid components.

The current simulations calculate the cask and fuel temperatures using two different methods. The first uses CFD simulations that include buoyancy-induced fluid motion. The other uses CFD but assumes the gas speed is zero (Stagnant-CFD or SCFD). Both these models include the effects of radiation heat transfer across the gas-filled regions. Comparison of these two results shows the effect of gas motion.

For all calculations a uniform volumetric heat generation was applied to all the fuel pellets. It was determined while the cask is subjected to Normal Conditions of transport [U.S. Nuclear Regulatory Commission 2002]. Those conditions are still air at an environment temperature of T_{ENV} = 38°C, with a constant, solar Heat Flux of 388 W/m² absorbed by the exterior package surface. In this work, the solar absorptivity is 0.57 and the package radiates to the environment with a surface emissivity of 0.2 [General Atomics 1998]. The natural convection heat flux at each surface location is calculated as q = h (T – T_{ENV}). In this expression T is the local surface temperature, and h is the heat transfer coefficient between the package surface and its surroundings. It is determined based on a Nusselt number correlation for a horizontal cylinder in stagnant air [Incropera and DeWitt 1996].

\[ h = \frac{k}{d} \left\{ 0.6 + \frac{0.387 \times Ra^{1/6}}{1 + \left[ \frac{0.559}{Pr} \right]^{9/16}} \right\}^{2/3} \]  

(1)

In this equation the package diameter is d = 2.42 m, k is the thermal conductivity of air, Pr = v/\alpha is the Prandtl number. The kinematic viscosity of air is v and thermal diffusivity is \alpha. The Rayleigh number is,

\[ Ra = \frac{g \beta (T_{\text{mid}} - T_{\text{ENV}}) d^3}{v \alpha} \]  

(2)

where g is the acceleration of gravity and \beta is the isobaric expansion coefficient of air.

Shaded Conditions

For this, the boundary conditions are the same as for the normal conditions of transport with still air at an environmental temperature of T_{ENV} = 38°C while the solar heat flux absorbed by the package exterior surface is set to zero.

Hypothetical Accident Conditions

The Code of Federal Regulations [U.S Nuclear Regulatory Commission 2002] specifies a fire heat transfer model that can be used to evaluate transport cask under fire accident conditions. It consists of a fully-engulfing fire temperature and emissivity of at least 800°C and 0.9, and appropriate
convection. For the current fire simulations, these lower limits are used along with a package surface emissivity of 0.8. For simplicity’s sake, convection heat transfer between the fire and package is not included (The sensitivity of the results to convection may be considered in future work). The Code of Federal Regulations specify a fire duration of D = 0.5 hr. In the current work, we calculate the package response for a range of fire durations.

The package temperatures at the end of the fire (time t = D) are used as the initial condition for a transient post-fire calculation. In this work, the post-fire environment is identical to the normal hot day conditions of transport. These simulations calculate the temperatures throughout the package after the fire.

RESULTS AND DISCUSSIONS

Normal Conditions of Transport

Figure 4 shows the temperature contours in the right side of the cask for a fuel heat generation rate of Q = 800 W/assembly with N₂ cover gas. The plots in Figure 4a and 4b are from the CFD and SCFD simulations, respectively. The CFD simulation includes buoyancy-induced gas motion. As a result, the hottest location in each fuel compartment in Figure 4a is above the diagonal line passing through the cask center. The gas motion also causes the upper surface of the bottom cask to be hotter than the lower surface of the upper basket. As a result, heat transfers from the lower to the upper fuel region, making the upper region hotter than the lower. The hottest cladding is located in the upper opening and is 217°C. The SCFD simulations do not include gas motion. Their temperature contours are symmetric about the horizontal and diagonal lines that pass through the cask center. For SCFD simulations the maximum temperature is located on the diagonal and is 220°C.

Figure 5 shows temperature versus radial distance from the cask center s, for both CFD and SCFD fuel models. These temperatures are along the diagonal lines shown in Fig 4. Figure 5 shows results for both N₂ and He cover gases. For both gases (He & N₂) and models (CFD & SCFD), the temperature profiles are nearly identical outside the fuel/cover gas region (s > 22.5 cm). However the temperatures within the fuel regions are not the same. For N₂, the SCFD model gives higher temperatures along the diagonal than the CFD model. Gas motion causes the difference. We note that the diagonal line passes through the hottest fuel rod for SCFD model, but is below the hottest fuel rod for CFD model. For He, the temperature profiles from the CFD and SCFD are nearly identical. This is because the conductivity of the He is sufficiently high that gas motion does not affect heat transfer. The environmental temperature, T_{ENV} = 38°C is also shown. The external surface heat transfer coefficient and the solar heat flux used in this calculation significantly affect the temperature between the environment and cask surface. Future work will consider the sensitivity of the results to these quantities.

Figure 6a shows the peak cladding and peak surface temperatures versus heat generation rate as predicted by CFD and SCFD models. The peak cladding limit temperature T_{CLAD,MAX} = 400°C and the peak surface limit temperature T_{SURF,MAX} = 85°C is also shown. The lines marked with T_{PC} show the peak cladding temperature versus heat generation rate with solar heat flux. The lines marked with T_{PS} show the peak surface temperature versus heat generation rate when the solar heat flux is set to zero. Results form He and N₂ cover gases are presented. The CFD and SCFD results are so close together they are indistinguishable on this plot. For He, the peak cladding temperature reaches the clad limit temperature (400°C) at Q = 3265 W/assembly with CFD simulations. It is 30% lower when N₂ is the cover gas due to nitrogen’s lower thermal conductivity. The values predicted by SCFD simulations are within 0.5% of those predicted by CFD calculations. The maximum cladding temperature without the solar heat flux is lower when He is the cover gas than for N₂. The peak surface temperature is nearly the same for both He and N₂ cover gases. For CFD model, the peak surface temperature is slightly higher when N₂ is the cover gas than for He.

Table 1 shows the cask thermal dissipation capacity, Q_{TDC} that causes the cladding to reach the maximum limit temperature, T_{CLAD,MAX} = 400°C. The CFD and SCFD models predict nearly the same Q_{TDC} values for both He and N₂ cover gases. This indicates that it may not be necessary to expend the computational resources required to calculate the fluid motion when calculating Q_{TDC} in future calculations.

Figure 6b shows the difference between peak cladding temperatures predicted by different models (CFD and SCFD) and grid refinements (Fine and Coarse). The lines labeled “He [Fine – Coarse]” and “N₂ [Fine - Coarse]” shows the difference between SCFD simulations using the Fine and Coarse grids for He and N₂ cover gases, respectively. It shows that the maximum difference between the fine and coarse grid is 0.5°C for He and 2.0°C for N₂. This difference is small compared to the difference between peak cladding temperature and surroundings. This indicates that the current simulations are mesh-independent. The other two lines show the difference between the SCFD and CFD results for He and N₂ cover gases. For N₂ cover gas, the SCFD calculations give slightly higher T_{PC} than the CFD simulations. This is expected because SCFD does not include fluid motion. For He, the CFD results are hotter than SCFD. However this difference is less than the difference between the fine and coarse grid results.

Figure 7 shows the basket wall temperatures versus wall coordinate, w. The plots show results for N₂ with Q = Q_{TDC} = 2500 W/assembly with SCFD model and Q = Q_{TDC} = 2515 W/assembly with CFD model, and He with Q = Q_{TDC} = 3265 W/assembly for both CFD and SCFD models. The w-coordinate is the distance along the basket surfaces in Figure 2. The value w = 0 corresponds to the corners closest to the package center, and w = 0.456 is at the corner farthest from the location. Results from the two upper basket cords are presented. The walls close to the cask center are hotter and exhibit larger temperature gradients than the surfaces near the package periphery. For N₂, the wall temperature varies from 213°C to 382°C for CFD and 190°C to 383°C for SCFD, while the variation for He is 209°C.
to 394°C for both CFD and SCFD. These results show that the compartment wall temperatures are highly non-isothermal.

Table 2 shows the cask thermal dissipation capacity, $Q_{TDC}$ that causes the surface to reach the maximum limit temperature, $T_{SURF, MAX} = 85°C$ without solar heat flux. For this case, the maximum cladding temperatures, $T_{PC,1}$ are different for both He and N$_2$.

**Regulatory Duration Fire, $D = 0.5$ hr.**

Figure 8 shows the peak cladding temperature response versus time in the upper fuel assembly for a 30-minute fire. A vertical dashed line shows the time at which the simulated fire ends and the post-fire conditions begin. The cladding temperatures begin to rise after the fire begins, and they continue to rise after the fire is extinguished before peaking and then slowly decreasing. This delay is due to heat continuing to diffuse to the fuel from the hotter regions in the periphery of the cask.

In this figure, the upper two lines indicate the maximum cladding temperatures for an initial peak clad temperature of 400°C. The maximum temperature of the clad after the fire is extinguished is same for CFD and SCFD models for both cover gases. However, the maximum clad temperature is less than the creep deformation temperature of the clad, $T_{CD} = 570°C$ in any of the models.

The bottom set of lines indicate the maximum cladding temperature when the initial surface temperature of the package is at 85°C. In this case, the peak clad temperature values are different for He and N$_2$ at time, $t = 0$.

**Long Duration Fires, $0.5$ hr $< D < 20$ hr**

Figure 9 shows Peak cladding temperature versus time duration for different fuel models and cover gases for different fire durations. The package temperature at the end of the fire (time $t = D$) are used as the initial conditions for a transient post-fire calculations. In the current work, we calculate the package response for a range of fire durations, $D$. The post-fire environment is identical to the normal hot day transport conditions at an environment temperature of $T_{ENV} = 38°C$ with a constant solar heat flux of $388$ W/m$^2$ absorbed by the entire exterior package surface. These simulations calculate the temperatures throughout the package during and after the fire.

Figure 10 is a plot of the peak clad post fire temperature versus fire duration for both fuel models (CFD and SCFD) and cover gases (He and N$_2$) for both the initial conditions, $T_{PC,1} = 400°C$ and $T_{PS,1} = 85°C$. Horizontal lines show two temperatures of concern for the cladding. The lower is the temperature limit for Creep Deformation, $T_{CD} = 570°C$. The upper line is the temperature limit for Burst Rupture, $T_{BR} = 750°C$. The durations of concern are the durations at which the peak cladding temperature curves in Figure 9 cross the horizontal lines for these temperatures.

Table 1 shows the durations of concern for clad to reach creep deformation and burst rupture temperatures with solar heat flux. The CFD and SCFD models predict nearly the same Creep Deformation & Burst Rupture values for both He and N$_2$ cover gases.

Figure 11 shows the peak cladding temperature (without Solar Heat Flux) versus time duration for different fuel models and cover gases for different fire durations. The package temperature at the end of the fire (time $t = D$) are used as the initial conditions for a transient post-fire calculations. In the current work, we calculate the package response for a range of fire durations, $D$. These simulations calculate the temperatures throughout the package during and after the fire.

Table 2 shows the durations of concern for clad to reach creep deformation and burst rupture temperatures without solar heat flux. The CFD and SCFD models predict almost nearly the same Creep Deformation & Burst Rupture values for both cover gases.

**CONCLUSIONS**

This work assesses the temperature and resulting containment integrity of the fuel cladding within a generic legal weight truck package during normal conditions of transport and regulatory format fires using geometrically-accurate fuel region models. The package studied in this work resembles a modern cask designed to transport four PWR fuel assemblies. This work uses a two-dimensional finite volume mesh that accurately represents the geometry of the cask, including the fuel inside the cask. Computational Fluid Dynamic (CFD) simulations are performed that calculate buoyancy-driven gas motion, as well as the natural convection and radiation heat transfer in the gas filled fuel regions. These results are compared to Stagnant-gas CFD (SCFD) simulations, that where the gas speed is set to zero, to evaluate the effect of gas motion.

During Normal Conditions of Transport, the fuel cladding temperature must not exceed 400°C to avoid the formation of radial hydrides. The Thermal Dissipation Capacity based on the cladding temperature, $Q_{TDC, C}$ is the maximum allowable heat generation rate that causes the fuel clad to reach this temperature. The current CFD simulations predict that $Q_{TDC, C} = 3265$ W/assembly when He is the cover gas. It is 30% lower when N$_2$ is the cover gas to N$_2$'s lower thermal conductivity. The values predicted by SCFD simulations are within 0.5% of those predicted by CFD calculations. This indicates that buoyancy-induced gas motion does not significantly affect the cladding temperature under these conditions, and the less computationally-intensive SCFD simulations are sufficiently accurate to predict cladding temperatures. Federal regulations also require that the cask surface temperature be below 85°C when the cask is in the shade. The fuel thermal dissipation capacity based on the surface temperature is $Q_{TDC, S} = 1040$ W/assembly when either N$_2$ or He is the cover gas. SCFD simulations results are with 0.3% of this value.

During a fire, the cladding may experience creep deformation or burst rupture if its temperature exceeds 570°C or 750°C, respectively. If the cladding temperature before a fire is 400°C, CFD simulations predict that the minimum durations of a regulatory format fire capable of bringing the cladding to
570°C or 750°C (critical fire durations) are \( D_c = 2.7 \text{ hr} \) and 7.2 hr, respectively, when He is the cover gas. When \( N_2 \) is the cover gas, the critical fire durations are 8.1 and 8.6% longer. This longer duration is caused by the lower fuel heat generation rate when \( N_2 \) is the cover gas. If the surface temperature is 85°C before the fire starts, then the critical fire durations for creep deformation and burst rupture are \( D_c = 4.7 \text{ hr} \) and 11.6 hr, respectively, when either He or \( N_2 \) are the cover gases. The values predicted by SCFD calculations are within 0.2% of those predicted by CFD simulations.

**FUTURE WORK**

Finite element models that employ Effective Thermal Conductivity (ETC) in the fuel regions are considered. A geometrically-accurate two-dimensional single fuel assembly within isothermal compartment walls is modeled. Finite element thermal simulations will be performed to determine the cladding temperature for a range of compartment wall temperatures and assembly heat generation rates. These results are used to determine a temperature-dependent effective thermal conductivity of the fuel region. These effective properties are then applied to a two-dimensional model of a legal weight truck cask with homogenized fuel regions. Steady state normal conditions of transport simulations will be performed for a range of fuel heat generation rates. The generation rate that brings the fuel cladding tubes to their radial hydride formation temperature will be determined. Transient regulatory fire accident simulations will be performed for a range for fire durations. The minimum fire durations that bring the fuel cladding to its creep deformation or burst rupture temperatures will be determined. These results will be compared to simulations which employ cask models with geometrically-accurate fuel region models.

**ACKNOWLEDGMENTS**

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**REFERENCES**


Fig 1: Cross section of a Legal Weight Truck cask that transports four spent PWR fuel assemblies.

Fig 2: Two-dimensional computational domain of an Legal Weight Truck cask: (a) half cask coarse grid, (b) Detail of Coarse Grid with 46,798 elements, and (c) Detail of Fine grid with 210,874 elements.

Fig 3: Temperature Dependent Thermal Conductivity values for materials used in the LWT transport cask model.

Fig 4: Normal hot-day temperature contours of a Legal Weight Truck cask for Q = 800 W/assembly with N₂ from two fuel region models: (a) CFD, T MAX = 217°C (b) S-CFD, T MAX = 220°C
Fig 5: Normal hot day temperature versus distance from center along the diagonal lines in Fig. 3 for $Q = 800$ W/assembly. The environment temperature is also shown.

Table 1: Heat Generation Rate and Durations of Concern with Solar Heat Flux for different Fuel models for $Q = Q_{TDC}$ for $N_2$ and He Cover gases.

<table>
<thead>
<tr>
<th>Cover Gas</th>
<th>$Q_{TDC, C}$ [W/assembly]</th>
<th>$D_C$ [hr]</th>
<th>CFD</th>
<th>SCFD</th>
</tr>
</thead>
<tbody>
<tr>
<td>Helium</td>
<td>3265</td>
<td>2.7, 7.2</td>
<td>CD</td>
<td>BR</td>
</tr>
<tr>
<td>Nitrogen</td>
<td>2515</td>
<td>3.3, 8.38</td>
<td>CD</td>
<td>BR</td>
</tr>
</tbody>
</table>

Table 2: Heat Generation Rate and Durations of Concern without Solar Heat Flux for different Fuel models for $Q = Q_{TDC}$ for $N_2$ and He cover gases.

<table>
<thead>
<tr>
<th>Cover Gas</th>
<th>$Q_{TDC, S}$ [W/assembly]</th>
<th>$T_{PC, I}$ [$^\circ$C]</th>
<th>$D_C$ [hr]</th>
<th>CFD</th>
<th>SCFD</th>
</tr>
</thead>
<tbody>
<tr>
<td>Helium</td>
<td>1040</td>
<td>186</td>
<td>4.7, 11.6</td>
<td>CD</td>
<td>BR</td>
</tr>
<tr>
<td>Nitrogen</td>
<td>1010</td>
<td>222</td>
<td>4.8, 11.7</td>
<td>CD</td>
<td>BR</td>
</tr>
</tbody>
</table>

Fig 6: (a) Peak Cladding temperature (with Solar Heat Flux) and Peak Surface temperature (without Solar Heat Flux) versus heat generation rate for different fuel models (CFD & SCFD) and cover gases (He & $N_2$). The fuel clad temperature and surface temperature limits are also shown. (b) Difference between peak cladding temperatures calculated using different fuel models (CFD & SCFD) and different grid refinements (Coarse & Fine).

Fig 7: Temperature profiles along walls of basket openings versus cord coordinate for $N_2$ at $Q = Q_{TDC,N_2} = 2500$ W/assembly for SCFD and $Q = Q_{TDC,N_2} = 2515$ W/assembly for CFD and He at $Q = Q_{TDC,He} = 3265$ W/assembly for CFD and SCFD models.
Fig 8: Peak cladding temperature (with Solar Heat Flux) and Peak Surface temperature (without Solar Heat Flux) versus time for different fuel models and cover gases for 30 minute fire duration.

Fig 9: Peak cladding temperature versus duration for different fuel models and cover gases for different fire durations.

Fig 10: Durations of Concern for different models and cover gases.

Fig 11: Peak cladding temperature versus duration for different fuel models and cover gases for different fire durations at peak surface temperature, $T_{\text{SURF,MAX}} = 85^\circ\text{C}$.