APPENDIX 3.AN: DYNA3D ANALYSES OF HI-TRAC SIDE DROPS AND IMPACT BY A LARGE TORNADO MISSILE

3.AN.1 INTRODUCTION

This appendix considers the HI-TRAC transfer cask response to two different transient accident events; namely, (1) a side drop onto a horizontal target surface from a specified height, and (2), a side impact from a large tornado missile. The analyses are performed as part of Load Cases 02.b and 04 (see Table 3.1.5), respectively. All dynamic analyses are performed using the dynamic finite element code DYNA3D (also known as LS-DYNA3D). This code has been approved for use in this class of problems by the NRC in previous submittals (HI-STAR 100), and has been benchmarked in an approved topical report. DYNA3D has also been used in Appendix 3.A to examine handling accidents involving a loaded MPC contained in the HI-STORM 100 overpack.

The first analysis in this appendix simulates a handling accident that results in a drop of the loaded HI-TRAC (Load Case 02.b in Table 3.1.5). The side drop accident considers the HI-TRAC in a horizontal orientation with its lowest point at a specified elevation above the target. Two initial orientations of the transfer cask are considered to bound all potential side drop accidents. For this case, the only loads considered to lead to high stresses are the inertia loads from the deceleration. It is noted that an alternate analysis of the handling accident has also been performed using a rigid body model of the HI-TRAC System to provide a confirmatory analysis.

The second analysis in this appendix simulates a strike on the HI-TRAC water jacket by a large tornado missile (Load Case 04 in Table 3.1.5). The consequences of a large tornado missile strike are examined by assuming that the vehicle strike is simulated by a specified impact force-time impulse applied over a fixed area of the water jacket. In this appendix, the impact force is considered as the only load on the HI-TRAC.

3.AN.2 HANDLING ACCIDENT - SIDE DROP

Handling accidents with a HI-TRAC transfer containing a loaded MPC are credible events only with HI-TRAC initially horizontal (Table 3.1.5). The stress analyses carried out in Chapter 3 of this safety analysis report assume that the inertial loading on the load bearing members of the MPC, the fuel basket, and the transfer cask due to a handling accident are limited by the Table 3.1.2 decelerations. The maximum deceleration experienced by a structural component is the product of the rigid body deceleration sustained by the structure and the dynamic load factor (DLF) applicable to that structural component. The dynamic load factor (DLF) is a function of the contact impulse and the structural characteristics of the component. A solution for dynamic load factors is provided in Appendix 3.X.

The rigid body deceleration is a strong function of the load-deformation characteristics of the impact interface, weight of the cask, and the drop height. For the HI-TRAC System, the weight of the structure and its surface compliance characteristics are known. However, the contact stiffness of the ISFSI pad (and other surfaces over which the HI-TRAC may be carried during its movement to the ISFSI) is site-dependent. The contact resistance of the collision interface, which is influenced by the HI-TRAC local compliance and the impacted surface compliance, therefore, is not known a priori for

a site. Analyses for the HI-TRAC body decelerations are presented here for a reference ISFSI pad (which is the pad used in a recent Lawrence Livermore National Laboratory report).

3.AN.2.2 Purpose

The purpose of this simulation is to demonstrate that the rigid body decelerations of the 125-ton and 100-ton HI-TRAC transfer casks are sufficiently low so that the design basis deceleration of 45g is not exceeded. Only one type of accidental drop (a side drop) of a loaded HI-TRAC transfer cask on the ISFSI pad is considered in this appendix. The loaded HI-TRAC, attached to the transfer lid, free-falls from a horizontal orientation (the transfer cask's longitudinal axis is horizontal) from a height "h", before impacting the horizontal target surface. The height, "h", is measured from the target surface up to the *lowest* point on the transfer cask system. For the side drop analyses in this appendix, "h", is

h = 42"

Two initial orientations for HI-TRAC are considered to bound the handling accident:

In scenario A, the cask impacts the target with the lowest point being the rotation trunnion. The cask has a primary impact between the lower rotation trunnion and the target pad and then a secondary impact between the water jacket and the upper trunnion and the target pad. Figure 3.AN.2 shows the orientation for this scenario after the end of the event.

In scenario B, the primary impact occurs between the transfer lid and the target pad with a secondary impact following between the water jacket and the pad. Figure 3.AN.1 shows the orientation for this scenario after the end of the event.

Scenario B, with the trunnions initially in a horizontal plane, represents the normal transfer orientation and maximizes the slapdown angle when secondary impact begins.

Scenario A, with the trunnions vertical, represents a handling accident where the transfer cask is assumed to rotate 90 degrees prior to target impact. This scenario insures that the rotation trunnion suffers a direct strike at primary impact and maximizes the potential for the involvement of the lifting trunnions in the secondary impact.

3.AN.2.3 Background and Methodology

The analysis of the HI-TRAC handling accident follows the similar analysis of the HI-STORM 100 accident evaluation. The methodology and the model is based on the work performed by Lawrence Livermore National Laboratory (LLNL) [3.AN.1, 3.AN.2]. Subsequently, USNRC personnel published a paper [3.AN.3] affirming the NRC's endorsement of the LLNL methodology. The LLNL simulation used modeling and simulation algorithms contained within the commercial computer code DYNA3D [3.AN.6].

Holtec has previously developed a finite element model for implementation on DYNA3D that is fully consistent with LLNL's cask model (including the use of the Butterworth filter for discerning rigid body deceleration from "noisy" impact data). The details of the DYNA3D dynamic model, as it has been applied to the HI-STAR 100 overpack are contained in a proprietary benchmark report [3.AN.4] wherein it is shown that the peak deceleration in every case of billet drop analyzed by LLNL is replicated within a small tolerance by the Holtec model. The case of the so-called "generic" cask, for which LLNL provided predicted response under side drop and tipover events, is also bounded by the Holtec model. In summary, the benchmarking effort documented in [3.AN.4] is in full compliance with the guidance of the Commission [3.AN.3].

Having developed and benchmarked an LLNL-consistent cask impact model, this model has been applied to prognosticate the HI-STAR 100 drop scenarios in a previous FSAR, and has been applied herein (see Appendix 3.A) to evaluate the HI-STORM 100 overpack performance during handling accidents.

In this section, the NRC approved target (reinforced concrete pad with underlying soil) is modeled together with the NRC approved MPC model. The HI-STORM 100 overpack is replaced with a finite element model of the HI-TRAC transfer cask. For the side drop scenario, considering the reference target (pad) elasto-plastic-damage characteristics, the object is threefold:

1. To demonstrate that the drop height "h" is such that the rigid body deceleration of the HI-TRAC, anywhere in the active fuel region, is below the 45g-design basis.
2. To demonstrate that the inner shell of the HI-TRAC does not suffer permanent deformation to the extent that ready retrievability of the contained MPC is compromised.
3. To demonstrate that global stresses in the HI-TRAC transfer cask, away from the impact interfaces, do not exceed the Level D stress intensities permitted by the ASME Code, Section III, Appendix F, for Class 3 NF components.

A description of the work effort and a summary of the results are presented in the following sections.

3.AN.2.4 Assumptions and Input Data

3.AN.2.4.1 Assumptions

The assumptions used to create the model are completely described in Reference [3.AN.4] and are shown there to be consistent with the LLNL simulation. There are two key aspects that are restated here:

The cask pad is assumed to be identical to the pad defined by LLNL [3.AN.2]. It is also identical to the pad utilized in the benchmark report [3.AN.4]. The essential data that defines the reference pad used to qualify the HI-TRAC System is provided in Table 3.AN.1.

3.AN.2.4.2 Input Data

Table 3.AN.1 characterizes the properties of the reference target pad used in the analysis. The inputs are taken from References [3.AN.2] and [3.AN.4].

Table 3.AN.2 details the geometry of the 100-ton and 125-ton HI-TRAC used in the side drop simulations. This data is taken from applicable HI-TRAC drawings and Tables in Section 3.2.

3.AN.2.5 Finite Element Models

Four finite-element models, corresponding to each of the postulated impact scenarios (A and B) pertinent to both types of casks (100-ton HI-TRAC and 125-ton HI-TRAC), are constructed using the pre-processor integrated with the DYNA3D software [3.AN.5]. A typical finite-element model is organized into 16 independent parts describing all structural components of the HI-TRAC System (the transfer lid plates, the bottom flange, the interior and exterior shell, the lead shielding, the top flange, the top lid, the lower and upper trunnions, the radial channels and outer closure plates of the water jacket), the MPC (steel plates and the basket fuel zone), and the concrete pad and the elastic soil stratum. Using symmetry, only a half finite-element model is constructed. The finite-element models used to numerically investigate the postulated side-drop scenarios are depicted in Figures 3.AN.3, 3.AN.4, and 3.AN.11.

The structural components of the HI-TRAC System are represented by elasto-plastic materials (*MAT_PIECEWISE_LINEAR_PLASTICITY), while the concrete pad and the soil stratum retain the material description used in the NRC approved HI-STAR 100 FSAR and also used in Appendix 3.A. for HI-STORM 100 overpack accident analyses.

The soil grid is a rectangular prism (800 inches long, 375 inches wide and 470 inches deep), and is constructed from 13294 solid type finite-elements. The material defining this part is an elastic orthotropic material. The central portion of the soil (400 inches long, 150 inches wide and 170 inches deep) where the stress concentration is expected to appear is discretized with a finer mesh.

The concrete pad is 320 inches long, 100 inches wide and is 36 inches thick. This part contains 8208 solid finite-elements. A uniform sized finite-element mesh is used to model the concrete pad. The concrete behavior is described using a special constitutive law and yielding surface (contained within DYNA3D). The geometry, the material properties, and the material behavior are identical to the LLNL reference pad.

The MPC and the contained fuel are modeled in two parts that represent the lid and baseplate, and the fuel area. An elastic material is used for both parts. The finite-element mesh pertinent to the MPC contains 1122 solid finite-elements. The MPC model is identical to that used in the cited handling accident simulations for the HI-STAR and HI-STORM overpacks. Gaps between the MPC and the transfer cask inner shell and lids are included in the model.

3.AN.2.6 Impact Velocity

For the side drop events, the impact velocity, v , is readily calculated from the Newtonian formula:

$$v = \sqrt{(2gh)}$$

where

g = acceleration due to gravity
h = free-fall height

The impact velocity, corresponding to a drop height of 42 inches, used in the numerical investigations presented in this appendix is 180.16 inch/second.

3.AN.2.7 Results

The DYNA3D deceleration time-history results are processed using a Butterworth filter (in conformance with the LLNL methodology and previously used in the HI-STAR 100 and HI-STORM 100 overpack analyses) to establish the rigid body deceleration of the HI-TRAC cask. All other outputs (displacements, forces) presented are directly (un-filtered) from the DYNA3D solver. A total of four simulations have been performed (2 casks with 2 initial orientations). The following "roadmap" summarizes the graphical results from the totality of simulations performed in support of the HI-TRAC transfer cask handling accident.

ITEM	HI-TRAC 125 – Scenario A	HI-TRAC 125 – Scenario B	HI-TRAC 100 – Scenario A	HI-TRAC 100 – Scenario B
Overall Model	Figure 3.AN.3	Figure 3.AN.11	-	-
HI-TRAC Mesh	Figure 3.AN.4	-	-	-
Z- Displacement at Transfer Lid, Top Lid	Figure 3.AN.5	Figure 3.AN.12	-	Figure 3.AN 20
Z- Deceleration at Centroid of Transfer Lid	Figure 3.AN.6	-	-	-
Z-Deceleration at Centroid of Inner Shell	Figure 3.AN.7	-	-	-
Z-Deceleration at Centroid of Top Lid	Figure 3.AN 8	-	-	-
Rigid Body Decelerations of Centroid of Transfer Lid, Inner Shell, and Top Lid	-	Figure 3.AN.13	Figure 3.AN.17	Figure 3.AN 21
Interface Force at Target/Primary and Secondary Impact Sites	Figure 3.AN.9	Figure 3.AN 14	Figure 3.AN.18	Figure 3.AN.22
Z-Displacements at Centroid of Inner Shell – Upper and Lower Points	Figure 3.AN.10	Figure 3.AN.15	Figure 3.AN.19	Figure 3.AN.23
Interface Force – Top Lid/MPC	-	Figure 3.AN.16	-	Figure 3.AN.24

Table 3.AN.3 presents the summary of all key results that are gleaned from the analyses. Within each data block in Table 3.AN.3, the specific figure number is given in parentheses. Where impact forces are reported in the tables, the reported value in Table 3.AN.3 has been doubled to reflect that the actual analysis model encompassed only one-half of the geometry. Table 3.AN.3 generally reports

peak values. However, there are three specific additional calculations that use the tabular results to derive additional information. In the section below, we demonstrate that:

1. The top lid never impacts the target.
2. The diametric change in the HI-TRAC inner shell diameter is such that the MPC retrievability is not compromised.
3. The interface force between the transfer lid and the HI-TRAC bottom flange can be computed from available data from the drop simulations.

To demonstrate that the top lid suffers no direct impact with the target, we examine the maximum vertical displacement of the top lid and the transfer lid (Figures 3.AN.5, 3.AN.12, and 3.AN.20). The allowable vertical displacement of the top lid (assuming no vertical displacement of the target pad) can be obtained from the drawings and bills-of-material for the HI-TRAC casks. Knowing the initial position of the lowest point on the top lid at the beginning of the event, we need only compare the allowable displacement plus any target pad displacement distance with the differential distance obtained from Figures 3.AN.5, 3.AN.12, and 3.AN.20. The following tabulation summarizes the results from inspection of the drawings and the figures:

ITEM	125-TON –Scenario A	125-TON Scenario B	100-TON Scenario B
Allowable Top Lid Vertical Displacement (from Drawings) Plus Target Vertical Deflection (inch)	-12.8469*	-28.77	-28.462
Top Lid Vertical Displacement (inch)	-9.75 (3.AN.5)	-27.3 (3.AN.12)	-27.5 (3.AN.20)
Transfer Lid Vertical Displacement (inch)	-2.0 (3.AN.5)	+1.0 (3.AN.12)	+2.25 (3.AN.20)
Maximum Angle of Inclination (Degrees)	2.31	8.46	8.93
Differential Vertical Displacement (inch)	-7.75	-28.3	-29.75

* Vertical deflection of target not included in this table value.

An estimate for the local deformation of the target under the secondary impact location is obtained from Figures 3.AN.29 and 3.AN.30, for example, and is included in the allowable top lid displacement in the columns associated with "Scenario B". These figures show the 100-ton HI-TRAC at the instant of maximum vertical deformation; the conclusion that the lid does not impact the target, as demonstrated in the table, is independently confirmed by Figure 3.AN.30. In the table above, the angle of inclination is computed as the angle whose "sin" is the differential vertical displacement divided by the distance between the measurement points (per Table 3.AN.2).

To demonstrate retrievability of the MPC, the change in the diameter of the inner shell of HI-TRAC can be computed from the DYNA3D output for absolute displacements of two opposing points on the

inner shell. Figure 3.AN.25 shows the geometry at the beginning of the event, and at a rotated position. The vertical movements V_T and V_B (a negative sign means displacement is toward target pad) are calculated by DYNA3D and shown in Figures 3.AN.10, 3.AN.15, 3.AN.19, and 3.AN.23. The rotation angle is computed in the tabulation above. The diametric decrease is $|U_B-U_T|$ and is computed from the following formula:

$$|U_B - U_T| = \frac{|V_B - V_T| - D(1-\cos(\theta))}{\cos(\theta)}$$

The following results are obtained using the results from DYNA3D and the preceding formula:

Maximum Change in Diameter of HI-TRAC from Secondary Impacts	
CASE	Diametric Change (inch)
125-Ton, Scenario A	0.228
125 Ton, Scenario B	0.113
100 Ton, Scenario B	0.067

The above diametric changes are less than the nominal gap (reduced by the thermal expansion effect calculated in Appendix 3.I). The above calculation, together with the fact that there is no evidence of global plastic straining of the inner shell at the end of the simulation, supports the conclusion that ready retrievability of the MPC is not impaired by the handling accident.

Finally, we outline the computation of the interface force between the HI-TRAC bottom flange and the transfer lid. Figure 3.4.29 in Section 3.4 shows a free-body of the transfer lid at primary impact. With reference to that figure, the equation of equilibrium is:

$$M_{TL} a_{TL} = F_I - G_I$$

where

M_{TL} = the mass of the transfer lid
a_{TL} = the time varying acceleration of the centroid of the transfer lid
F_I = the time varying contact force at the interface with the target
G_I = the time varying interface force at the bottom flange/transfer lid interface

Solving for the interface force give the result

$$G_I = F_I - M_{TL} a_{TL}$$

Using the appropriate transfer lid mass and acceleration, together with the target interface force at the limiting time instant, provides values for the interface force. Using results from Table 3.AN.3 and transfer lid bounding weights from Table 3.2.2 gives the following results for peak interface forces:

HI-TRAC BOTTOM FLANGE/TRANSFER LID INTERFACE FORCE	
CASE	INTERFACE FORCE (kips)
125-Ton, Scenario A	1,183
125-Ton, Scenario B	1,272
100-Ton, Scenario A	1,129
100-Ton, Scenario B	1,070

Finally, we note that decelerations obtained from the DYNA3D numerical solutions are filtered through a Butterworth type filter identical to the filter used by LLNL to investigate the "generic" cask [3.AN.2]. The filter has the following characteristics: 350 Hz passband frequency, 10,000 Hz stopband frequency, 0.15 maximum passband ripple, and 10 minimum stopband attenuation.

The computer code utilized in this analysis is LS-DYNA3D [3.AN.5] validated under Holtec's QA system.

3.AN.3 LARGE TORNADO MISSILE IMPACT

3.AN.3.1 Model

The finite element model used in the side drop analysis is used with the following modifications:

a. The target is eliminated from the model and the HI-TRAC is restrained at the ends to equilibrate any applied missile impact force.

b. The large tornado missile impact is simulated by a total input force-time relationship applied at nodes encompassing an interface area on the water jacket. The total force is apportioned to the nodes lying within and on the boundary of the interface area. The force-time relation is obtained from a NRC approved topical report [3.AN.7]. The interface contact area, appropriate to the large missile, is obtained from [3.AN.8]. The force-time relation (during the rise to a maximum value), is given by the expression [3.AN.7, Equation. D-6]:

$$F(t) = 0.625 V_s W_m \sin(20t)$$

$$V_s = 184.6 \text{ ft./sec.}$$

$$W_m = 3960 \text{ lb.}$$

The time "t" in the formula is in "seconds".

Figure 3.AN.26 shows the interface force-time data imposed on the HI-TRAC water jacket. The interface area was assumed approximately mid-way along the length of the cask.

3.AN.3.2 Results from Analysis

Figures 3.AN.27 and 3.AN.28 show the Von Mises stress distribution in the water jacket for both HI-TRAC transfer casks at the instant when the applied interface force peaks. Table 3.AN.4 summarizes results from these figures as well as the strain data from the two simulations. No plastic strain occurs in the inner shell due to the impact in either simulation.

3.AN.4 COMPUTER CODES AND ARCHIVAL INFORMATION

The input and output files created to perform the analyses reported in this appendix are listed for future retrievability.

The computer code utilized in this analysis is DYNA3D [3.AN.5] validated under Holtec's QA system.

The DYNA3D computer code has an extensive finite-element and material description library and can account for various time-dependent contact conditions that normally arise between the various structural components during the impact analysis.

The input and the output files created are stored on Holtec's server disk and tape archived as required by Holtec's QA procedures under the following address:

F:\PROJECTS\5014\HITRAC\....

Each one of the subdirectories contains specific data related to the analyzed drop scenarios and is organized in five files: DYNA3D input file (XXX.DYN), corresponding to the analyzed drop event, and four time-history files (MATSUM- the impactor velocity time-history, RCFORC- the impact force time-history, NODOUT- displacement, velocity and acceleration and PLOT- the model deformation time-history) generated during the numerical analysis.

All DYNA3D simulations were performed in a PC environment (Windows 98), using a Dell Corporation Pentium II - 450 MHz computer.

3.AN.5 CONCLUSIONS

The DYNA3D analysis of HI- TRAC reported in this appendix leads to the following conclusions:

a. If a loaded HI-TRAC, with its longitudinal axis horizontal, undergoes a free fall for a height of 42 inches and impacts a reference pad defined by Table 3.AN.1, the maximum rigid body deceleration at primary or secondary impact is below the design basis of 45g's. Therefore, since the design basis deceleration is 45 g', it is concluded that there will be no adverse effect on the fuel basket, within the MPC, by this handling accident.

b. The maximum stress intensity in the HI-TRAC transfer cask is below Level D allowables during the side drop event and during the impact by a large tornado missile.

c. The diametric change of the HI-TRAC inner shell is less than the minimum gap between the MPC and the inner shell of the HI-TRAC transfer cask. Therefore, after either a side drop or an impact by a large tornado missile, ready retrievability of the MPC is not adversely affected.

Tables 3.AN.3 and 3.AN.4 provide key results for all drop cases studied herein with additional results provided within the discussion.

3.AN.6 REFERENCES

[3.AN.1] Witte, M., et al., "Evaluation of Low-Velocity Impacts Tests of Solid Steel Billet onto Concrete Pads.", Lawrence Livermore National Laboratory, UCRL-ID-126274, Livermore, California, March 1997.

[3.AN.2] Witte, M., et al., "Evaluation of Low-Velocity Impacts Tests of Solid Steel Billet onto Concrete Pads, and Application to Generic ISFSI Storage Cask for Tipover and Side Drop.", Lawrence Livermore National Laboratory, UCRL-ID-126295, Livermore, California, March 1997.

[3.AN.3] Tang, D.T., Raddatz, M.G., and Sturz, F.C., "NRC Staff Technical Approach for Spent Fuel Cask Drop and Tipover Accident Analysis", SFPO, USNRC (1997).

[3.AN.4] Simulescu, I., "Benchmarking of the Holtec LS-DYNA3D Model for Cask Drop Events", Holtec Report HI-971779, September 1997.

[3.AN.5] LS-DYNA3D, Version 936-03, Livermore Software Technology Corporation, September 1996.

[3.AN.6] Whirley, R.G., "DYNA3D, A Nonlinear, Explicit, Three-Dimensional Finite element Code for Solid and Structural Mechanics - User Manual.", Lawrence Livermore National Laboratory, UCRL-MA-107254, Revision 1, 1993.

[3.AN.7] Design of Structures for Missile Impact, BC-TOP-9A, Revision 2, Bechtel Power Corporation Topical Report, September, 1974.

[3.AN.8] Missiles Generated by Natural Phenomena, NUREG-0800, SRP 3.5.1.4.

Table 3.AN.1: Essential Variables to Characterize the Reference Target

Thickness of concrete	36 inches
Nominal compressive strength of concrete	4,200 psi
Concrete mass density	2.097E-04 lb-sec^2/in^4
Concrete shear modulus	1.514E+06 psi
Concrete Poisson's ratio	0.22
Mass density of the engineered fill (soil)	1.498E-04 lb-sec^2/in^4
Modulus of elasticity of the soil	28,000 psi
Poisson's ratio of the soil	0.3

Note: The concrete Young's Modulus is derived from the American Concrete Institute recommended formula $57{,}000\sqrt{f}$ where f is the nominal compressive strength of the concrete (psi).

Table 3.AN.2: Key Cask Input Data in Analyses

ITEM	HI-TRAC -125	HI-TRAC – 100
Total HI-TRAC Weight	152,636 lb.	109,214 lb.
Lead Weight	79,109 lb.	49,810 lb.
Overall Length of the Transfer Cask	207.875 inches	204.125 inches
Length x Width of Transfer Lid*	128 in. x 93 in.	128 in. x 89 in.
Outside Diameter of the Radial Channels	94.625 inches	91.0 inches
Inner Shell Diameter	68.75 inches	68.75 inches
Outer Radius of Top Lid	40.625 inches	39.0 inches
Longitudinal Distance Between Point on Transfer Lid and Point on Top Lid where Vertical Displacements are measured (inch)	192.25 inches	191.60 inches
MPC Weight (including fuel)	88,857 lb.	88,857 lb.
MPC Height	190.5 inches	190.5 inches
MPC Diameter	68.375 inches	68.375 inches
MPC Bottom Plate Thickness	2.5 inches	2.5 inches
MPC Top Plate Thickness	9.5 inches	9.5 inches

* We note that the intermediate plate extends 2" beyond the length and provides the initial site for impact for the "Scenario B" orientation.

Table 3.AN.3: Side Drop Analyses Results

ITEM	HI-TRAC 125 – Scenario A	HI-TRAC 125 – Scenario B	HI-TRAC 100 – Scenario A	HI-TRAC 100 – Scenario B
Maximum Vertical Deceleration during Primary Impact (g's) –Transfer Lid	23.5 (3.AN.6)	30.8 (3.AN.13)	31.75 (3.AN.17)	35.0 (3.AN.21)
Vertical Deceleration at Top Lid at Instant of Max. g's Primary Impact (g's)	-1.0 (3.AN.8)	-9.0 (3.AN.13)	-3.0 (3.AN.17)	-8.0 (3.AN.21)
Max. Interface Force Target/Primary Impact Site (kips)	1,700 (3.AN.9)	1,950 (3.AN.14)	1,700 (3 AN.18)	1,700 (3.AN.22)
Maximum Vertical Deceleration at Centroid – Instant of Maximum Primary Impact Force on Target(g's)	6.0 (3.AN.7)	7.0 (3.AN.13)	12.5 (3.AN.17)	7.0 (3.AN.21)
Vertical Deceleration of Transfer Lid at Instant of Max g's Secondary Impact (g's)	6.25 (3.AN.6, 3.AN.8)	-3.0 (3.AN.13)	-3.5 (3.AN.17)	-7.0 (3.AN.21)
Maximum Vertical Deceleration at Top Lid - Secondary Impact (g's)	32.0 (3.AN.8)	25.5 (3.AN.13)	45.0 (3.AN.17)	36.5 (3.AN.21)
Vertical Deceleration at Centroid at Instant of Max. g's Secondary Impact (g's)	13 (3.AN.7, 3.AN.8)	9.0 (3.AN.13)	17.5 (3.AN.17)	10.0 (3.AN.21)
Max. Interface Force Target/Secondary Impact Site (kips)	1,850 (3.AN.9)	1,300 (3.AN.14)	1,450 (3 AN.18)	1,500 (3.AN.22)
Max. Von Mises Stress (ksi)	38 367	37.577	40.444	40.690
Inner Shell Plastic Strain	0.002818	0.001146	0.006631	0.00492
Maximum MPC/Top Lid Interface Force (kips)	-	132 (3.AN.16)	-	39.0 (3.AN.24)
Max. Difference in Absolute Vertical Displacement of Opposing Points on Inner Shell (inch)	0 27 (3.AN.10)	0.5 (3.AN.15)	0.55 (3.AN.19)	1.1 (3.AN.23)

Table 3.AN.4 Large Tornado Missile Impact Analysis Results

ITEM	CALCULATED VALUE –125 TON	CALCULATED VALUE – 100 TON	ALLOWABLE VALUE
Maximum Stress Intensity in Water Jacket (ksi)	19.073	28.331	58.7
Maximum Stress Intensity in Inner Shell (ksi)	6.023	11.467	58.7
Maximum Plastic Strain in Water Jacket	0.0	0.0000932	-
Maximum Plastic Strain in Inner Shell	0.0	0.0	-

4.4 THERMAL EVALUATION FOR NORMAL CONDITIONS OF STORAGE

Under long-term storage conditions, the HI-STORM System (i.e., HI-STORM overpack and MPC) thermal evaluation is performed with the MPC cavity backfilled with helium. Thermal analysis results for the long-term storage scenarios are obtained and reported in this section.

4.4.1 Thermal Model

The MPC basket design consists of four distinct geometries to hold 24 or 32 PWR, or 68 BWR fuel assemblies. The basket is a matrix of square compartments designed to hold the fuel assemblies in a vertical position. The basket is a honeycomb structure of alloy steel (Alloy X) plates with full-length edge-welded intersections to form an integral basket configuration. All individual cell walls, except outer periphery cell walls in the MPC-68 and MPC-32, are provided with Boral neutron absorber sandwiched between the box wall and a stainless steel sheathing plate over the full length of the active fuel region.

The design basis decay heat generation (per PWR or BWR assembly) for long-term normal storage is specified in Table 2.1.6. The decay heat is conservatively considered to be non-uniformly distributed over the active fuel length based on the design basis axial burnup distributions provided in Chapter 2 (Table 2.1.11).

Transport of heat from the interior of the MPC to its outer surface is accomplished by a combination of conduction through the MPC basket metal grid structure, and conduction and radiation heat transfer in the relatively small helium gaps between the fuel assemblies and basket cell walls. Heat dissipation across the gap between the MPC basket periphery and the MPC shell is by a combination of helium conduction, natural convection (by means of the "Rayleigh" effect)† radiation across the gap and conduction in the aluminum alloy 1100 heat conduction elements*. MPC internal helium circulation is recognized in the thermal modeling analyses reported herein. Heat rejection from the outer surface of the MPC to the environment is primarily accomplished by convective heat transfer to a buoyancy driven airflow through the MPC-to-overpack annular gap. Inlet and outlet ducts in the overpack cylinder at its bottom and top, respectively, allow circulation of air through the annulus. A secondary heat rejection path from the outer surface of the MPC to the environment involves thermal radiation heat transfer across the annular gap, radial conduction through the overpack cylinder, and natural convection and thermal radiation from the outer surface of the overpack to the atmosphere.

† Neglected in the thermal analyses for conservatism.
* Neglected in the thermal analyses for conservatism.

4.4.1.1 Analytical Model - General Remarks

Transport of heat from the heat generation region (fuel assemblies) to the outside environment (ambient air or ground) is analyzed broadly in terms of three interdependent thermal models.

1. The first model considers transport of heat from the fuel assembly to the basket cell walls. This model recognizes the combined effects of conduction (through helium) and radiation, and is essentially a finite element technology based update of the classical Wooton & Epstein [4.4.1] (which considered radiative heat exchange between fuel rod surfaces) formulation.

2. The second model considers heat transport within an MPC cross section by conduction and radiation. The effective cross sectional thermal conductivity of the basket region, obtained from a combined fuel assembly/basket heat conduction-radiation model developed on ANSYS, is applied to an axisymmetric thermal model of the HI-STORM System on the FLUENT [4.1.2] code.

3. The third model deals with the transmission of heat from the MPC exterior surface to the external environment (heat sink). The upflowing air stream in the MPC/cask annulus extracts most of the heat from the external surface of the MPC, and a small amount of heat is radially deposited on the HI-STORM inner surface by conduction and radiation. Heat rejection from the outside cask surfaces to ambient air is considered by accounting for natural convection and radiative heat transfer mechanisms from the vertical (cylindrical shell) and top cover (flat) surfaces. The reduction in radiative heat exchange between cask outside vertical surfaces and ambient air, because of blockage from the neighboring casks arranged for normal storage at an ISFSI pad as described in Section 1.4, is recognized in the analysis. The overpack top plate is modeled as a heated surface in convective and radiative heat exchange with air and as a recipient of heat input through insolation. Insolation on the cask surfaces is based on 12-hour levels prescribed in 10CFR71, averaged over a 24-hour period, after accounting for partial blockage conditions on the sides of the overpack.

Subsections 4.4.1.1.1 through 4.4.1.1.9 contain a systematic description of the mathematical models devised to articulate the temperature field in the HI-STORM System. The description begins with the method to characterize the heat transfer behavior of the prismatic (square) opening referred to as the "fuel space" with a heat emitting fuel assembly situated in it. The methodology utilizes a finite element procedure to replace the heterogeneous SNF/fuel space region with an equivalent solid body having a well-defined temperature-dependent conductivity. In the following subsection, the method to replace the "composite" walls of the fuel basket cells with an equivalent "solid" wall is presented. Having created the mathematical equivalents for the SNF/fuel spaces and the fuel basket walls, the method to represent the MPC cylinder containing the fuel basket by an equivalent cylinder whose thermal conductivity is a function of the spatial location and coincident temperature is presented.

Following the approach of presenting descriptions starting from the inside and moving to the outer region of a cask, the next subsections present the mathematical model to simulate the overpack. Subsection 4.4.1.1.9 concludes the presentation with a description of how the different models for the specific regions within the HI-STORM System are assembled into the final FLUENT model.

4.4.1.1.1 Overview of the Thermal Model

Thermal analysis of the HI-STORM System is performed by assuming that the system is subject to its maximum heat duty with each storage location occupied and with the heat generation rate in each stored fuel assembly equal to the design-basis maximum value. While the assumption of equal heat generation imputes a certain symmetry to the cask thermal problem, the thermal model must incorporate three attributes of the physical problem to perform a rigorous analysis of a fully loaded cask:

i. While the rate of heat conduction through metals is a relatively weak function of temperature, radiation heat exchange is a nonlinear function of surface temperatures.

ii. Heat generation in the MPC is axially non-uniform due to non-uniform axial burnup profiles in the fuel assemblies.

iii. Inasmuch as the transfer of heat occurs from inside the basket region to the outside, the temperature field in the MPC is spatially distributed with the maximum values reached in the central core region.

It is clearly impractical to model every fuel rod in every stored fuel assembly explicitly. Instead, the cross section bounded by the inside of the storage cell, which surrounds the assemblage of fuel rods and the interstitial helium gas, is replaced with an "equivalent" square (solid) section characterized by an effective thermal conductivity. Figure 4.4.1 pictorially illustrates the homogenization concept. Further details of this procedure for determining the effective conductivity are presented in Subsection 4.4.1.1.2; it suffices to state here that the effective conductivity of the cell space will be a function of temperature because the radiation heat transfer (a major component of the heat transport between the fuel rods and the surrounding basket cell metal) is a strong function of the temperatures of the participating bodies. Therefore, in effect, every storage cell location will have a different value of effective conductivity (depending on the coincident temperature) in the homogenized model. The temperature-dependent fuel assembly region effective conductivity is determined by a finite volume procedure, as described in Subsection 4.4.1.1.2.

In the next step of homogenization, a planar section of MPC is considered. With each storage cell inside space replaced with an equivalent solid square, the MPC cross section consists of a metallic gridwork (basket cell walls with each square cell space containing a solid fuel cell square of effective thermal conductivity, which is a function of temperature) circumscribed by a circular ring (MPC shell). There are five distinct materials in this section, namely the homogenized fuel cell squares, the Alloy X structural materials in the MPC (including Boral sheathing), Boral, Alloy 1100 aluminum heat conduction elements, and helium gas. Each of the five constituent materials in this section has a different conductivity. It is emphasized that the conductivity of the homogenized fuel cells is a strong function of temperature.

In order to replace this thermally heterogeneous MPC section with an equivalent conduction-only region, resort to the finite element procedure is necessary. Because the rate of transport of heat within

the MPC is influenced by radiation, which is a temperature-dependent effect, the equivalent conductivity of the MPC region must also be computed as a function of temperature. Finally, it is recognized that the MPC section consists of two discrete regions, namely, the basket region and the peripheral region. The peripheral region is the space between the peripheral storage cells and the MPC shell. This space is essentially full of helium surrounded by Alloy X plates and optionally Alloy 1100 aluminum heat conduction elements. Accordingly, as illustrated in Figure 4.4.2 for MPC-68, the MPC cross section is replaced with two homogenized regions with temperature-dependent conductivities. In particular, the effective conductivity of the fuel cells is subsumed into the equivalent conductivity of the basket cross section. The finite element procedure used to accomplish this is described in Subsection 4.4.1.1.4. The ANSYS finite element code is the vehicle for all modeling efforts described in the foregoing.

In summary, appropriate finite-element models are used to replace the MPC cross section with an equivalent two-region homogeneous conduction lamina whose local conductivity is a known function of coincident absolute temperature. Thus, the MPC cylinder containing discrete fuel assemblies, helium, Boral and Alloy X, is replaced with a right circular cylinder whose material conductivity will vary with radial and axial position as a function of the coincident temperature. Finally, HI-STORM is simulated as a radially symmetric structure with a buoyancy-induced flow in the annular space surrounding the heat generating MPC cylinder.

The thermal analysis procedure described above makes frequent use of equivalent thermal properties to ease the geometric modeling of the cask components. These equivalent properties are rigorously calculated values based on detailed evaluations of actual cask system geometries. All these calculations are performed conservatively to ensure a bounding representation of the cask system. This process, commonly referred to as submodeling, yields accurate (not approximate) results. Given the detailed nature of the submodeling process, experimental validation of the individual submodels is not necessary.

Internal circulation of helium in the sealed MPC is modeled as flow in a porous media in the fueled region containing the SNF (including top and bottom plenums). The basket-to-MPC shell clearance space is modeled as a helium filled radial gap to include the downcomer flow in the thermal model. The downcomer region, as illustrated in Figure 4.4.2, consists of an azimuthally varying gap formed by the square-celled basket outline and the cylindrical MPC shell. At the locations of closest approach a differential expansion gap (a small clearance on the order of 1/10 of an inch) is engineered to allow free thermal expansion of the basket. At the widest locations, the gaps are on the order of the fuel cell opening (~6" (BWR) and ~9" (PWR) MPCs). It is heuristically evident that heat dissipation by conduction is maximum at the closest approach locations (low thermal resistance path) and that convective heat transfer is highest at the widest gap locations (large downcomer flow). In the FLUENT thermal model, a radial gap that is large compared to the basket-to-shell clearance and small compared to the cell opening is used. As a relatively large gap penalizes heat dissipation by conduction and a small gap throttles convective flow, the use of a single gap in the FLUENT model understates both conduction and convection heat transfer in the downcomer region. Heat dissipation by the inclusion of aluminum heat conduction elements, as stated earlier, is conservatively neglected in the HI-STORM thermal modeling.

The FLUENT thermal modeling methodology has been benchmarked with full-scale cask test data (EPRI TN-24P cask testing), as well as with PNNL's COBRA-SFS modeling of the HI-STORM System. The benchmarking work has been documented in a Holtec topical report HI-992252 ("Topical Report on the HI-STAR/HI-STORM Thermal Model and Its Benchmarking with Full-Size Cask Test Data").

In this manner, a loaded MPC standing upright on the ISFSI pad in a HI-STORM overpack is replaced with a right circular cylinder with spatially varying temperature-dependent conductivity. Heat is generated within the basket space in this cylinder in the manner of the prescribed axial burnup distribution. In addition, heat is deposited from insolation on the external surface of the overpack. Under steady state conditions the total heat due to internal generation and insolation is dissipated from the outer cask surfaces by natural convection and thermal radiation to the ambient environment and from heating of upward flowing air in the annulus. Details of the elements of mathematical modeling are provided in the following.

4.4.1.1.2 Fuel Region Effective Thermal Conductivity Calculation

Thermal properties of a large number of PWR and BWR fuel assembly configurations manufactured by the major fuel suppliers (i.e., Westinghouse, CE, B&W, and GE) have been evaluated for inclusion in the HI-STORM System thermal analysis. Bounding PWR and BWR fuel assembly configurations are determined using the simplified procedure described below. This is followed by the determination of temperature-dependent properties of the bounding PWR and BWR fuel assembly configurations to be used for cask thermal analysis using a finite volume (FLUENT) approach.

To determine which of the numerous PWR assembly types listed in Table 4.4.1 should be used in the thermal model for the PWR fuel baskets (MPC-24, MPC-24E, MPC-32), we must establish which assembly type has the maximum thermal resistance. The same determination must be made for the MPC-68, out of the menu of SNF types listed in Table 4.4.2. For this purpose, we utilize a simplified procedure that we describe below.

Each fuel assembly consists of a large array of fuel rods typically arranged on a square layout. Every fuel rod in this array is generating heat due to radioactive decay in the enclosed fuel pellets. There is a finite temperature difference required to transport heat from the innermost fuel rods to the storage cell walls. Heat transport within the fuel assembly is based on principles of conduction heat transfer combined with the highly conservative analytical model proposed by Wooton and Epstein [4.4.1]. The Wooton-Epstein model considers radiative heat exchange between individual fuel rod surfaces as a means to bound the hottest fuel rod cladding temperature.

Transport of heat energy within any cross section of a fuel assembly is due to a combination of radiative energy exchange and conduction through the helium gas that fills the interstices between the fuel rods in the array. With the assumption of uniform heat generation within any given horizontal cross section of a fuel assembly, the combined radiation and conduction heat transport effects result in the following heat flow equation:

$$Q = \sigma C_o F_\varepsilon A [T_C^4 - T_B^4] + 13.5740 L K_{cs} [T_C - T_B]$$

where:

F_ε = Emissivity Factor

$$= \frac{1}{(\frac{1}{\varepsilon_C} + \frac{1}{\varepsilon_B} - 1)}$$

ε_C, ε_B = emissivities of fuel cladding, fuel basket (see Table 4.2.4)

C_o = Assembly Geometry Factor

$$= \frac{4N}{(N+1)^2} \text{ (when N is odd)}$$

$$= \frac{4}{N+2} \text{ (when N is even)}$$

N = Number of rows or columns of rods arranged in a square array
A = fuel assembly "box" heat transfer area = 4 × width × length
L = fuel assembly length
K_{cs} = fuel assembly constituent materials volume fraction weighted mixture conductivity
T_C = hottest fuel cladding temperature (°R)
T_B = box temperature (°R)
Q = net radial heat transport from the assembly interior
σ = Stefan-Boltzmann Constant (0.1714×10^{-8} Btu/ft^2-hr-°R^4)

In the above heat flow equation, the first term is the Wooten-Epstein radiative heat flow contribution while the second term is the conduction heat transport contribution based on the classical solution to the temperature distribution problem inside a square shaped block with uniform heat generation [4.4.5]. The 13.574 factor in the conduction term of the equation is the shape factor for two-dimensional heat transfer in a square section. Planar fuel assembly heat transport by conduction occurs through a series of resistances formed by the interstitial helium fill gas, fuel cladding and enclosed fuel. An effective planar mixture conductivity is determined by a volume fraction weighted sum of the individual constituent material resistances. For BWR assemblies, this formulation is applied to the region inside the fuel channel. A second conduction and radiation model is applied between the channel and the fuel basket gap. These two models are combined, in series, to yield a total effective conductivity.

The effective conductivity of the fuel for several representative PWR and BWR assemblies is presented in Tables 4.4.1 and 4.4.2. At higher temperatures (approximately 450°F and above), the zircaloy clad fuel assemblies with the lowest effective thermal conductivities are the W-17×17 OFA (PWR) and the GE11-9×9 (BWR). A discussion of fuel assembly conductivities for some of the recent vintage 10×10 array and certain plant specific BWR fuel designs is presented near the end of

this subsection. As noted in Table 4.4.2, the Dresden 1 (intact and damaged) fuel assemblies are excluded from consideration. The design basis decay heat load for Dresden-1 intact and damaged fuel (Table 2.1.7) is approximately 58% lower than the MPC-68 design-basis maximum heat load (Table 2.1.6). Examining Table 4.4.2, the effective conductivity of the damaged Dresden-1 fuel assembly in a damaged fuel container is approximately 40% lower than the bounding (GE-11 9×9) fuel assembly. Consequently, the fuel cladding temperatures in the HI-STORM System with Dresden-1 intact or damaged fuel assemblies will be bounded by design basis fuel cladding temperatures. Based on this simplified analysis, the W-17×17 OFA PWR and GE11-9×9 BWR fuel assemblies are determined to be the bounding configurations for analysis of zircaloy clad fuel at design basis maximum heat loads. As discussed in Section 4.3.1, stainless clad fuel assemblies with significantly lower decay heat emission characteristics are not deemed to be bounding.

For the purpose of determining axial flow resistance for inclusion of MPC thermosiphon effect in the HI-STORM system modeling, equivalent porous media parameters for the W-17x17OFA and GE11-9x9 fuels are computed. Theoretically bounding expansion and contraction loss factors are applied at the grid spacer locations to conservatively maximize flow resistance. As an additional measure of conservatism, the grids are modeled by postulating that they are formed using thick metal sheets which have the effect of artificially throttling flow. Heat transfer enhancement by grid spacers turbulation is conservatively ignored in the analysis.

Having established the governing (most resistive) PWR and BWR SNF types, we use a finite-volume code to determine the effective conductivities in a conservative manner. Detailed conduction-radiation finite-volume models of the bounding PWR and BWR fuel assemblies developed on the FLUENT code are shown in Figures 4.4.3 and 4.4.4, respectively. The PWR model was originally developed on the ANSYS code, which enables individual rod-to-rod and rod-to-basket wall view factor calculations to be performed using the AUX12 processor. Limitations of radiation modeling techniques implemented in ANSYS do not permit taking advantage of quarter symmetry of the fuel assembly geometry. Unacceptably long CPU time and large workspace requirements necessary for performing gray body radiation calculations for a complete fuel assembly geometry on ANSYS prompted the development of an alternate simplified model on the FLUENT code. The FLUENT model is benchmarked with the ANSYS model results for a Westinghouse 17×17 fuel assembly geometry for the case of black body radiation (emissivities = 1). The FLUENT model is found to yield conservative results in comparison to the ANSYS model for the "black" surface case. The FLUENT model benchmarked in this manner is used to solve the gray body radiation problem to provide the necessary results for determining the effective thermal conductivity of the governing PWR fuel assembly. The same modeling approach using FLUENT is then applied to the governing BWR fuel assembly, and the effective conductivity of GE-11 9×9 fuel determined.

The combined fuel rods-helium matrix is replaced by an equivalent homogeneous material that fills the basket opening by the following two-step procedure. In the first step, the FLUENT-based fuel assembly model is solved by applying equal heat generation per unit length to the individual fuel rods and a uniform boundary temperature along the basket cell opening inside periphery. The temperature difference between the peak cladding and boundary temperatures is used to determine an effective conductivity as described in the next step. For this purpose, we consider a two-dimensional cross section of a square shaped block with an edge length of 2L and a uniform volumetric heat

source (q_g), cooled at the periphery with a uniform boundary temperature. Under the assumption of constant material thermal conductivity (K), the temperature difference (ΔT) from the center of the cross section to the periphery is analytically given by [4.4.5]:

$$\Delta T = 0.29468 \frac{q_g L^2}{K}$$

This analytical formula is applied to determine the effective material conductivity from a known quantity of heat generation applied in the FLUENT model (smeared as a uniform heat source, q_g) basket opening size and ΔT calculated in the first step.

As discussed earlier, the effective fuel space conductivity must be a function of the temperature coordinate. The above two-step analysis is carried out for a number of reference temperatures. In this manner, the effective conductivity as a function of temperature is established.

In Table 4.4.5, 10×10 array type BWR fuel assembly conductivity results from a simplified analysis are presented to determine the most resistive fuel assembly in this class. The Atrium-10 fuel type is determined to be the most resistive in this class of fuel assemblies. A detailed finite-element model of this assembly type was developed to rigorously quantify the heat dissipation characteristics. The results of this study are presented in Table 4.4.6 and compared to the BWR bounding fuel assembly conductivity depicted in Figure 4.4.5. The results of this study demonstrate that the bounding fuel assembly conductivity is conservative with respect to the 10×10 class of BWR fuel assemblies.

Table 4.4.23 summarizes plant specific fuel types' effective conductivities. From these analytical results, SPC-5 is determined to be the most resistive fuel assembly in this group of fuel. A finite element model of the SPC-5 fuel assembly was developed to confirm that its in-plane heat dissipation characteristics are bounded from below by the Design Basis BWR fuel conductivities used in the HI-STORM thermal analysis.

Temperature-dependent effective conductivities of PWR and BWR design basis fuel assemblies (most resistive SNF types) are shown in Figure 4.4.5. The finite volume results are also compared to results reported from independent technical sources. From this comparison, it is readily apparent that FLUENT-based fuel assembly conductivities are conservative. The FLUENT computed values (not the published literature data) are used in the MPC thermal analysis presented in this document.

4.4.1.1.3 Effective Thermal Conductivity of Boral/Sheathing/Box Wall Sandwich

Each MPC basket cell wall (except the MPC-68 and MPC-32 outer periphery cell walls) is manufactured with a Boral neutron absorbing plate for criticality control. Each Boral plate is sandwiched in a sheathing-to-basket wall pocket. A schematic of the "Box Wall-Boral-Sheathing" sandwich geometry of an MPC basket is illustrated in Figure 4.4.6. During fabrication, a uniform normal pressure is applied to each "Box Wall-Boral-Sheathing" sandwich in the assembly fixture during welding of the sheathing periphery on the box wall. This ensures adequate surface-to-surface contact for elimination of any macroscopic air gaps. The mean coefficient of linear expansion of the Boral is higher than the thermal expansion coefficients of the basket and sheathing materials. Consequently, basket heat-up from the stored SNF will further ensure a tight fit of the Boral plate in the sheathing-to-box pocket. The presence of small microscopic gaps due to less than perfect surface

finish characteristics requires consideration of an interfacial contact resistance between the Boral and box-sheathing surfaces. A conservative contact resistance resulting from a 2 mil Boral to pocket gap is applied in the analysis. In other words, no credit is taken for the interfacial pressure between Boral and stainless plate/sheet stock produced by the fixturing and welding process.

Heat conduction properties of a composite "Box Wall-Boral-Sheathing" sandwich in the two principal basket cross sectional directions as illustrated in Figure 4.4.6 (i.e., lateral "out-of-plane" and longitudinal "in-plane") are unequal. In the lateral direction, heat is transported across layers of sheathing, air-gap, Boral (B_4C and cladding layers) and box wall resistances that are essentially in series (except for the small helium filled end regions shown in Figure 4.4.7). Heat conduction in the longitudinal direction, in contrast, is through an array of essentially parallel resistances comprised of these several layers listed above. For the ANSYS based MPC basket thermal model, corresponding non-isotropic effective thermal conductivities in the two orthogonal sandwich directions are determined and applied in the analysis.

These non-isotropic conductivities are determined by constructing two-dimensional finite-element models of the composite "Box Wall-Boral-Sheathing" sandwich in ANSYS. A fixed temperature is applied to one edge of the model and a fixed heat flux is applied to the other edge, and the model is solved to obtain the average temperature of the fixed-flux edge. The equivalent thermal conductivity is the obtained using the resulting temperature difference across the sandwich as input to a one-dimensional Fourier equation as follows:

$$K_{eff} = \frac{q \times L}{T_h - T_c}$$

where:

Keff = effective thermal conductivity
q = heat flux applied in the ANSYS model
L = ANSYS model heat transfer path length
T_h = ANSYS calculated average edge temperature
T_c = specified edge temperature

The heat transfer path length will vary, depending on the direction of transfer (i.e., in-plane or out-of-plane).

4.4.1.1.4 Modeling of Basket Conductive Heat Transport

The total conduction heat rejection capability of a fuel basket is a combination of planar and axial contributions. These component contributions are calculated independently for each MPC basket design and then combined to obtain an equivalent isotropic thermal conductivity value.

The planar heat rejection capability of each MPC basket design (i.e., MPC-24, MPC-68, MPC-32 and MPC-24E) is evaluated by developing a thermal model of the combined fuel assemblies and composite basket walls geometry on the ANSYS finite element code. The ANSYS model includes a geometric layout of the basket structure in which the basket "Box Wall-Boral-Sheathing" sandwich is replaced by a "homogeneous wall" with an equivalent thermal conductivity. Since the thermal

conductivity of the Alloy X material is a weakly varying function of temperature, the equivalent "homogeneous wall" must have a temperature-dependent effective conductivity. Similarly, as illustrated in Figure 4.4.7, the conductivities in the "in-plane" and "out-of-plane" directions of the equivalent "homogeneous wall" are different. Finally, as discussed earlier, the fuel assemblies and the surrounding basket cell openings are modeled as homogeneous heat generating regions with an effective temperature dependent in-plane conductivity. The methodology used to reduce the heterogeneous MPC basket - fuel assemblage to an equivalent homogeneous region with effective thermal properties is discussed in the following.

Consider a cylinder of height, L, and radius, r_o, with a uniform volumetric heat source term, q_g, insulated top and bottom faces, and its cylindrical boundary maintained at a uniform temperature, T_c. The maximum centerline temperature (T_h) to boundary temperature difference is readily obtained from classical one-dimensional conduction relationships (for the case of a conducting region with uniform heat generation and a constant thermal conductivity K_s):

$$(T_h - T_c) = q_g r_o^2/(4 K_s)$$

Noting that the total heat generated in the cylinder (Q_t) is $\pi r_o^2 L q_g$, the above temperature rise formula can be reduced to the following simplified form in terms of total heat generation per unit length (Q_t/L):

$$(T_h - T_c) = (Q_t / L)/ (4 \pi K_s)$$

This simple analytical approach is employed to determine an effective basket cross-sectional conductivity by applying an equivalence between the ANSYS finite element model of the basket and the analytical case. The equivalence principle employed in the thermal analysis is depicted in Figure 4.4.2. The 2-dimensional ANSYS finite element model of the MPC basket is solved by applying a uniform heat generation per unit length in each basket cell region (depicted as Zone 1 in Figure 4.4.2) and a constant basket periphery boundary temperature, T_c'. Noting that the basket region with uniformly distributed heat sources and a constant boundary temperature is equivalent to the analytical case of a cylinder with uniform volumetric heat source discussed earlier, an effective MPC basket conductivity (K_{eff}) is readily derived from the analytical formula and ANSYS solution leading to the following relationship:

$$K_{eff} = N (Q_f'/L) / (4 \pi [T_h' - T_c'])$$

where:

N = number of fuel assemblies
(Q_f'/L) = per fuel assembly heat generation per unit length applied in ANSYS model
T_h' = peak basket cross-section temperature from ANSYS model

Cross sectional views of MPC basket ANSYS models are depicted in Figures 4.4.9 and 4.4.10. Notice that many of the basket supports and all shims have been conservatively neglected in the models. This conservative geometry simplification, coupled with the conservative neglect of thermal expansion that would minimize the gaps, yields conservative gap thermal resistances. Temperature-

dependent equivalent thermal conductivities of the fuel regions and composite basket walls, as determined from analysis procedures described earlier, are applied to the ANSYS model. The planar ANSYS conduction model is solved by applying a constant basket periphery temperature with uniform heat generation in the fuel region. The equivalent planar thermal conductivity values are lower bound values because, among other elements of conservatism, the effective conductivity of the most resistive SNF types (Tables 4.4.1 and 4.4.2) is used in the MPC finite element simulations.

The basket in-plane conductivities are computed for intact fuel storage and containerized fuel stored in Damaged Fuel Containers (DFCs). The MPC-24E is provided with four enlarged cells designated for storing damaged fuel. The MPC-68 has sixteen peripheral locations for damaged fuel storage in generic DFC designs. As a substantial fraction of the basket cells are occupied by intact fuel, the overall effect of DFC fuel storage on the basket heat dissipation rate is quite small. Including the effect of reduced conductivity of the DFC cells in MPC-24E, the basket conductivity is computed to drop slightly (~0.6%). In a bounding calculation in which all cells of MPC-68 are assumed occupied by fuel in DFC, the basket conductivity drops by about 5%. Conservatively, assuming 95% of intact fuel basket heat load adequately covers damaged fuel storage in the MPC-24E and MPC-68.

The axial heat rejection capability of each MPC basket design is determined by calculating the area occupied by each material in a fuel basket cross-section, multiplying by the corresponding material thermal conductivity, summing the products and dividing by the total fuel basket cross-sectional area. In accordance with NUREG-1536 guidelines, the only portion of the fuel assemblies credited in these calculations is the fuel rod cladding.

Having obtained planar and axial effective thermal conductivity contributions as described above, an equivalent isotropic thermal conductivity that yields the same overall heat transfer can be obtained. Two-dimensional conduction heat transfer in relatively short cylinders cannot be readily evaluated analytically, so an alternate approach is used herein.

Instead of computing precise isotropic conductivities, an RMS function of the planar and axial effective thermal conductivity values is used as follows:

$$k_{iso} = \sqrt{\frac{k_{rad}^{2} + k_{ax}^{2}}{2}}$$

where:

k_{iso} = equivalent isotropic thermal conductivity
k_{rad} = equivalent planar thermal conductivity
k_{ax} = equivalent axial thermal conductivity

This formulation has been benchmarked for specific application to the MPC basket designs and found to yield conservative equivalent isotropic thermal conductivities and, subsequently, conservative temperature results from subsequent thermal analyses.

Table 4.4.3 summarizes the isotropic MPC basket thermal conductivity values used in the subsequent cask thermal modeling. It should be noted that the isotropic conductivities calculated as

described above are actually higher than those reported in Table 4.4.3, imparting additional conservatism to the subsequent calculations.

4.4.1.1.5 Heat Transfer in MPC Basket Peripheral Region

Both of the MPC designs for storing PWR or BWR fuel are provided with relatively large regions, formed between the relatively cooler MPC shell and hot basket peripheral panels, filled with helium gas. Heat transfer in these helium-filled regions corresponds to the classical case of heat transfer in a differentially heated closed cavity. Many investigators, including Eckert and Carlson (Int. J. Heat Mass Transfer, vol. 2, p. 106, 1961) and Elder (J. Fluid Mech., vol. 23, p. 77, 1965) have performed experimental studies of this arrangement. The peripheral region between the basket and MPC inner surface is simulated as a tall fluid-filled cavity of height H formed between two differentially heated surfaces (ΔT) separated by a small distance L. In a closed cavity, an exchange of hot and cold fluids occurs near the top and bottom ends of the cavity, resulting in a net transport of heat across the gap. The rate of heat transfer across the cavity is characterized by a Rayleigh number, Ra_L, defined as:

$$Ra_L = \frac{C_p \rho^2 g \beta \Delta T L^3}{\mu K}$$

where:

C_p	=	fluid heat capacity
ρ	=	fluid density
g	=	acceleration due to gravity
β	=	coefficient of thermal expansion (equal to reciprocal of absolute temperature for gases)
ΔT	=	temperature difference between the hot and cold surfaces
L	=	spacing between the hot and cold surfaces
μ	=	fluid viscosity
K	=	fluid conductivity

Hewitt et al. [4.4.6] recommends the following Nusselt number correlation for heat transport in tall cavities:

$$Nu_L = 0.42\, Ra_L^{1/4}\, Pr^{0.012} \left(\frac{H}{L}\right)^{-0.3}$$

where Pr is the Prandtl number of the cavity fill gas.

A Nusselt number of unity implies heat transfer by fluid conduction only, while a higher than unity Nusselt number is due to the "Rayleigh" effect which monotonically increases with increasing Rayleigh number. Nusselt numbers applicable to helium-filled PWR and BWR fueled HI-STORM MPC peripheral voids used in the original licensing analysis are provided in Table 4.4.4. For conservatism, however, the contribution of the Rayleigh effect is ignored in the thermal model of the MPC.

4.4.1.1.6 Effective Thermal Conductivity of MPC Basket-to-Shell Aluminum Heat Conduction Elements

As shown in HI-STORM System MPC drawings in Section 1.5, an option for insertion of full-length heat conduction elements fabricated from thin aluminum Alloy 1100 sheet metal is shown in the MPC design drawings. Due to the high thermal conductivity of aluminum Alloy 1100 (about 15 times that of Alloy X), a significant rate of net heat transfer is possible along thin plates. Figure 4.4.11 shows the mathematical idealization of a typical conduction element inserted in a basket periphery panel-to-MPC shell space. The aluminum heat conduction element is shown to cover the MPC basket Alloy X peripheral panel and MPC shell (Regions I and III depicted in Figure 4.4.11) surfaces along the full-length of the basket except for isolated locations where fitup or inteference with other parts precludes complete basket coverage. Heat transport to and from the aluminum heat conduction element is conservatively postulated to occur across a thin helium gap as shown in the figure (i.e., no credit is taken for contact between the aluminum heat conduction element and the Alloy X fuel basket). Aluminum surfaces inside the hollow region are sandblasted prior to fabrication to result in a rough surface finish which has a significantly higher emissivity compared to smooth surfaces of rolled aluminum. The untreated aluminum surfaces directly facing Alloy X panels have a smooth finish to minimize contact resistance.

Net heat transfer resistance from the hot basket periphery panel to the relatively cooler MPC shell along the aluminum heat conduction element pathway is a sum of three individual resistances, in regions labeled I, II, and III in Figure 4.4.11. In Region I, heat is transported from the basket to the aluminum heat conduction element surface directly facing the basket panel across a thin helium resistance gap. Longitudinal transport of heat (in the z direction) in the aluminum plate (in Region I) will result in an axially non-uniform temperature distribution. Longitudinal one-dimensional heat transfer in the Region I aluminum plate was analytically formulated to result in the following ordinary differential equation for the non-uniform temperature distribution:

$$t\,K_{Al}\frac{\partial^2 T}{\partial z^2} = -\frac{K_{He}}{h}(T_h - T)$$

Boundary Conditions

$$\frac{\partial T}{\partial z} = 0 \text{ at } z = 0$$

$$T = T_h' \text{ at } z = P$$

where (see Figure 4.4.11):

$T(z)$ = non-uniform aluminum metal temperature distribution
t = heat conduction element thickness
K_{Al} = heat conduction element conductivity
K_{He} = helium conductivity
h = helium gap thickness
T_h = hot basket temperature
T_h' = heat conduction element Region I boundary temperature at $z = P$
P = heat conduction element Region I length

Solution of this ordinary differential equation subject to the imposed boundary condition is:

$$(T_h - T) = (T_h - T_h') \left[\frac{e^{\frac{z}{\sqrt{\alpha}}} + e^{-\frac{z}{\sqrt{\alpha}}}}{e^{\frac{P}{\sqrt{\alpha}}} + e^{-\frac{P}{\sqrt{\alpha}}}} \right]$$

where α is a dimensional parameter equal to ($h \times t \times K_{Al}/K_{he}$). The net heat transfer (Q_I) across the Region I helium gap can be determined by the following integrated heat flux to a heat conduction element of length L as:

$$Q_I = \int_0^P \frac{K_{He}}{h} (T_h - T)(L)\, dz$$

Substituting the analytical temperature distribution result obtained in Equation c, the following expression for net heat transfer is obtained:

$$Q_I = \frac{K_{He} L \sqrt{\alpha}}{h} \left(1 - \frac{1}{e^{\frac{P}{\sqrt{\alpha}}} + e^{-\frac{P}{\sqrt{\alpha}}}} \right) (T_h - T_h')$$

Based on this result, an expression for Region I resistance is obtained as shown below:

$$R_I = \frac{T_h - T_{h'}}{Q_I} = \frac{h}{K_{He} L \sqrt{\alpha}} \left(1 - \frac{1}{e^{\frac{P}{\sqrt{\alpha}}} + e^{-\frac{P}{\sqrt{\alpha}}}} \right)^{-1}$$

The Region II resistance expression can be developed from the following net heat transfer equation in the vertical leg of the conduction element as shown below:

$$Q_{II} = \frac{K_{Al} L t}{W} (T_h' - T_c')$$

where W is the conduction element Region II length.

$$R_{II} = \frac{T_h' - T_c'}{Q_{II}} = \frac{W}{K_{Al} L t}$$

Similarly, a Region III resistance expression can be analytically determined as shown below:

$$R_{III} = \frac{(T_c' - T_c)}{Q_{III}}$$

$$= \frac{h}{K_{He} L \sqrt{\alpha}} \left(1 - \frac{1}{e^{\frac{P}{\sqrt{\alpha}}} + e^{-\frac{P}{\sqrt{\alpha}}}} \right)^{-1}$$

This completes the analysis for the total thermal resistance attributable to the heat conduction elements, which is equal to the sum of the three individual resistances. The total heat conduction element resistance is smeared across the basket-to-MPC shell region as an effective uniform annular gap conductivity (see Figure 4.4.2). We note that heat transport along the conduction elements is an independent conduction path in parallel with conduction and radiation mechanisms in the large helium gaps. Helium conduction and radiation in the MPC basket-to-MPC shell peripheral gaps is accounted for separately in the ANSYS models for the MPCs, described earlier. Therefore, the net

conductivity of the MPC basket-to-MPC shell peripheral gap region is the sum of the heat conduction elements effective conductivity and the helium gap conduction-radiation effective conductivity. For conservatism, however, the contribution of the heat conduction elements is ignored in the HI-STORM thermal analyses.

4.4.1.1.7 Annulus Air Flow and Heat Exchange

The HI-STORM storage overpack is provided with four inlet ducts at the bottom and four outlet ducts at the top. The ducts are provided to enable relatively cooler ambient air to flow through the annular gap between the MPC and storage overpack in the manner of a classical "chimney". Hot air is vented from the top outlet ducts to the ambient environment. Buoyancy forces induced by density differences between the ambient air and the heated air column in the MPC-to-overpack annulus sustain airflow through the annulus.

In contrast to a classical chimney, however, the heat input to the HI-STORM annulus air does not occur at the bottom of the stack. Rather, the annulus air picks up heat from the lateral surface of the MPC shell as it flows upwards. The height dependent heat absorption by the annulus air must be properly accounted for to ensure that the buoyant term in the Bernoulli equation is not overstated making the solution unconservative. To fix ideas, consider two cases of stack heat input; Case A where the heat input to the rising air is all at the bottom (the "fireplace" scenario), and Case B, where the heat input is uniform along the entire height (more representative of the ventilated cask conditions). In both cases, we will assume that the air obeys the perfect gas law; i.e., at constant pressure, $\rho = C/T$ where ρ and T are the density and the absolute temperature of the air and C is a constant.

Case A: Entire Heat Input at the Bottom

In a stack of height H, where the temperature of the air is raised from T_i to T_o at the bottom (Figure 4.4.12; Case A), the net fluid "head" p_I is given by:

$$p_I = \rho_i H - \rho_o H$$

ρ_I and ρ_o are the densities of air corresponding to absolute temperatures T_i and T_o, respectively.

Since $\rho_i = \frac{C}{T_i}$ and $\rho_o = \frac{C}{T_o}$, we have:

$$p_I = CH\left(\frac{1}{T_i} - \frac{1}{T_o}\right)$$

or

$$p_I = \frac{CH\,\Delta T}{T_i T_o}$$

where: $\Delta T = T_o - T_i$

Let $\Delta T << Ti$, then we can write:

$$\frac{1}{T_o} = \frac{1}{T_i\,(1+\frac{\Delta T}{T_i})}$$

$$= \frac{1}{T_i}\left[1 - \frac{\Delta T}{T_i} + ...\right]$$

Substituting in the above we have:

$$p_1 = \frac{CH}{T_i}\,\delta\,(1 - \delta + ...)$$

where $\delta = \frac{\Delta T}{T_i}$ (dimensionless temperature rise)

or $p_1 = \rho_i\, H\, \delta - O(\delta^2)$.

<u>Case B: Uniform Heat Input</u>

In this case, the temperature of air rises linearly from T_i at the bottom to T_o at the top (Figure 4.4.12; Case B):

$$T_o = T_i + \zeta h\,;\; 0 \le h \le H$$

where:

$$\zeta = \frac{T_o - T_i}{H} = \frac{\delta T_i}{H}$$

The total buoyant head, in this case, is given by:

$$p_2 = \rho_i\, H - \int_0^H \rho\, dh$$

$$= \rho_i\, H - C\int_0^H \frac{1}{T}\, dh$$

$$= \rho_i\, H - C\int_0^H \frac{dh}{(T_i + \zeta h)}$$

$$= \rho_i\, H - \frac{C}{\zeta}\,\ell n\,(1+\delta)$$

Using the logarithmic expansion relationship and simplifying we have:

$$p_2 = \frac{\rho_i\, H\, \delta}{2} - O(\Delta^2)$$

Neglecting terms of higher order, we conclude that p_2 is only 50% of p_1, i.e., the buoyancy driver in the case of uniformly distributed heat input to the air is half of the value if the heat were all added at the bottom.

In the case of HI-STORM, the axial heat input profile into the annulus air will depend on the temperature difference between the MPC cylindrical surface and the rising air along the height (Case C in Figure 4.4.12). The MPC surface temperature profile, of course, is a strong function of the axial decay heat generation profile in the SNF. Previous analyses show that the HI-STORM "chimney" is less than 50% as effective as a classical chimney. As we explain in Subsection 4.4.1.1.9, this fact is fully recognized in the global HI-STORM thermal model implementation of FLUENT.

4.4.1.1.8 Determination of Solar Heat Input

The intensity of solar radiation incident on an exposed surface depends on a number of time varying terms. The solar heat flux strongly depends upon the time of the day as well as on latitude and day of the year. Also, the presence of clouds and other atmospheric conditions (dust, haze, etc.) can significantly attenuate solar intensity levels. Rapp [4.4.2] has discussed the influence of such factors in considerable detail.

Consistent with the guidelines in NUREG-1536 [4.4.10], solar input to the exposed surfaces of the HI-STORM overpack is determined based on 12-hour insolation levels recommended in 10CFR71 (averaged over a 24-hour period) and applied to the most adversely located cask after accounting for partial blockage of incident solar radiation on the lateral surface of the cask by surrounding casks. In reality, the lateral surfaces of the cask receive solar heat depending on the azimuthal orientation of the sun during the course of the day. In order to bound this heat input, the lateral surface of the cask is assumed to receive insolation input with the solar insolation applied horizontally into the cask array. The only reduction in the heat input to the lateral surface of the cask is due to partial blockage offered by the surrounding casks. In contrast to its lateral surface, the top surface of HI-STORM is fully exposed to insolation without any mitigation effects of blockage from other bodies. In order to calculate the view factor between the most adversely located HI-STORM system in the array and the environment, a conservative geometric simplification is used. The system is reduced to a concentric cylinder model, with the inner cylinder representing the HI-STORM unit being analyzed and the outer shell representing a reflecting boundary (no energy absorption).

Thus, the radius of the inner cylinder (R_i) is the same as the outer radius of a HI-STORM overpack. The radius of the outer cylinder (R_o) is set such that the rectangular space ascribed to a cask is preserved. This is further explained in the next subsection. It can be shown that the view factor from the outer cylinder to the inner cylinder ($F_{o\text{-}i}$) is given by [4.4.3]:

$$F_{o\text{-}i} = \frac{1}{R} - \frac{1}{\pi R} \times [\cos^{-1}(\frac{B}{A}) - \frac{1}{2L}\{\sqrt{(A+2)^2 - (2R)^2} \times \cos^{-1}(\frac{B}{RA}) + B\sin^{-1}(\frac{1}{R}) - \frac{\pi A}{2}\}]$$

where:

F_{o-i} = View Factor from the outer cylinder to the inner cylinder
R = Outer Cylinder Radius to Inner Cylinder Radius Ratio (R_o/R_i)
L = Overpack Height to Radius Ratio
$A = L^2 + R^2 - 1$
$B = L^2 - R^2 + 1$

Applying the theorem of reciprocity, the view factor (F_{i-a}) from outer overpack surface, represented by the inner cylinder, to the ambient can be determined as:

$$F_{i-a} = 1 - F_{o-i}\frac{R_o}{R_i}$$

Finally, to bound the quantity of heat deposited onto the HI-STORM surface by insolation, the absorptivity of the cask surfaces is assumed to be unity.

4.4.1.1.9 FLUENT Model for HI-STORM

In the preceding subsections, a series of analytical and numerical models to define the thermal characteristics of the various elements of the HI-STORM System are presented. The thermal modeling begins with the replacement of the Spent Nuclear Fuel (SNF) cross section and surrounding fuel cell space with a solid region with an equivalent conductivity. Since radiation is an important constituent of the heat transfer process in the SNF/storage cell space, and the rate of radiation heat transfer is a strong function of the surface temperatures, it is necessary to treat the equivalent region conductivity as a function of temperature. Because of the relatively large range of temperatures in a loaded HI-STORM System under the design basis heat loads, the effects of variation in the thermal conductivity of the Alloy X basket wall with temperature are included in the numerical analysis model. The presence of significant radiation effects in the storage cell spaces adds to the imperative to treat the equivalent storage cell lamina conductivity as temperature-dependent.

Numerical calculations and FLUENT finite-volume simulations have been performed to establish the equivalent thermal conductivity as a function of temperature for the limiting (thermally most resistive) BWR and PWR spent fuel types. Utilizing the most limiting SNF (established through a simplified analytical process for comparing conductivities) ensures that the numerical idealization for the fuel space effective conductivity is conservative for all non-limiting fuel types.

Having replaced the fuel spaces by solid square blocks with a temperature-dependent conductivity essentially renders the basket into a non-homogeneous three-dimensional solid where the non-homogeneity is introduced by the honeycomb basket structure composed of interlocking basket panels. The basket panels themselves are a composite of Alloy X cell wall, Boral neutron absorber, and Alloy X sheathing metal. A conservative approach to replace this composite section with an equivalent "solid wall" was described earlier.

In the next step, a planar section of the MPC is considered. The MPC contains a non-symmetric

basket lamina wherein the equivalent fuel spaces are separated by the "equivalent" solid metal walls. The space between the basket and the MPC, called the peripheral gap, is filled with helium gas. At this stage in the thermal analysis, the SNF/basket/MPC assemblage has been replaced with a two-zone (Figure 4.4.2) cylindrical solid whose thermal conductivity is a strong function of temperature.

The fuel assembly and MPC basket effective conductivity evaluations are performed for two distinct scenarios described earlier in this section. In the first scenario, the MPC cavity is backfilled with helium only. In the second scenario, gaseous fission products from a hypothetical rupture of 10% of the stored fuel rods dilute the backfill helium gas. As previously stated, thermal analysis results for both scenarios are obtained and reported in this section.

The thermal model for the HI-STORM overpack is prepared as a three-dimensional axisymmetric body. For this purpose, the hydraulic resistances of the inlet ducts and outlet ducts, respectively, are represented by equivalent axisymmetric porous media. Two overpack configurations are evaluated – HI-STORM 100 and a shorter variation (HI-STORM 100S) overpack. HI-STORM 100S features a smaller inlet duct-to-outlet duct separation and an optional enhanced gamma shield cross plat. Since the optional gammas shield cross plate flow resistance is bounding, the optional design was conservatively evaluated in the thermal analysis.The fuel cladding temperatures for MPC emplaced in a HI-STORM 100S overpack are confirmed to be bounded by the HI-STORM 100 System thermal model solution. Thus, separate table summaries for HI-STORM 100S overpack are not provided. The axial resistance to airflow in the MPC/overpack annulus (which includes longitudinal channels to "cushion" the stresses in the MPC structure during a postulated non-mechanistic tip-over event) is replaced by a hydraulically equivalent annulus. The surfaces of the ducts and annulus are assumed to have a relative roughness (ε) of 0.001. This value is appropriate for rough cast iron, wood stave and concrete pipes, and is bounding for smooth painted surfaces (all readily accessible internal and external HI-STORM overpack carbon steel surfaces are protected from corrosion by painting or galvanization). Finally, it is necessary to describe the external boundary conditions to the overpack situated on an ISFSI pad. An isolated HI-STORM will take suction of cool air from and reject heated air to, a semi-infinite half-space. In a rectilinear HI-STORM array, however, the unit situated in the center of the grid is evidently hydraulically most disadvantaged, because of potential interference to air intake from surrounding casks. To simulate this condition in a conservative manner, we erect a hypothetical cylindrical barrier around the centrally local HI-STORM. The radius of this hypothetical cylinder, R_o, is computed from the equivalent cask array downflow hydraulic diameter (D_h) which is obtained as follows:

where:

$$D_h = \frac{4 \times \text{Flow Area}}{\text{Wetted Perimeter}}$$

$$= \frac{4\left(A_o - \frac{\pi}{4} d_o^{\,2}\right)}{\pi d_o}$$

$A_o =$ Minimum tributary area ascribable to one HI-STORM (see Figure 4.4.24).

d_o = HI-STORM overpack outside diameter

The hypothetical cylinder radius, R_o, is obtained by adding half D_h to the radius of the HI-STORM overpack. In this manner, the hydraulic equivalence between the cask array and the HI-STORM overpack to hypothetical cylindrical annulus is established.

For purposes of the design basis analyses reported in this chapter, the tributary area A_o is assumed to be equal to 346 sq. ft. Sensitivity studies on the effect of the value of A_o on the thermal performance of the HI-STORM System shows that the system response is essentially insensitive to the assumed value of the tributary area. For example, a thermal calculation using A_o=225 sq. ft. (corresponding to 15 ft. square pitch) and design basis heat load showed that the peak cladding temperature is less than 1 °C greater than that computed using A_o = 346 sq. ft Therefore, the distance between the vertically arrayed HI-STORMs in an ISFSI should be guided by the practical (rather than thermal) considerations, such as personnel access to maintain air ducts or painting the cask external surfaces.

The internal surface of the hypothetical cylinder of radius R_o surrounding the HI-STORM module is conservatively assumed to be insulated. Any thermal radiation heat transfer from the HI-STORM overpack to this insulated surface will be perfectly reflected, thereby bounding radiative blocking from neighboring casks. Then, in essence, the HI-STORM module is assumed to be confined in a large cylindrical "tank" whose wall surface boundaries are modeled as zero heat flux boundaries. The air in the "tank" is the source of "feed air" to the overpack. The air in the tank is replenished by ambient air from above the top of the HI-STORM overpacks. There are two sources of heat input to the exposed surface of the HI-STORM overpack. The most important source of heat input is the internal heat generation within the MPC. The second source of heat input is insolation, which is conservatively quantified in the manner of the preceding subsection.

The FLUENT model consisting of the axisymmetric 3-D MPC space, the overpack, and the enveloping tank is schematically illustrated in Figure 4.4.13. The HI-STORM thermosiphon-enabled solution is computed in a two-step process. In the first step, a HI-STORM overpack thermal model computes the ventilation effect from annulus heating by MPC decay heat. In this model, heat dissipation is conservatively restricted to the MPC shell (i.e., heat dissipation from MPC lid and baseplate completely neglected. This modeling assumption has the effect of overstating the MPC shell, annulus air and concrete temperatures. In the next step, the temperature of stored fuel in a pressurized helium canister (thermosiphon model) is determined using the overpack thermal solution in the first step to fashion a bounding MPC shell temperature profile for the MPC thermal model. The modeling details are provided in the Holtec benchmarking report [4.4.12].A summary of the essential features of this model is presented in the following:

- A conservatively lower bound canister pressure of 5 atm is postulated for the thermosiphon modeling.

- Heat input due to insolation is applied to the top surface and the cylindrical surface of the overpack with a bounding maximum solar absorbtivity equal to 1.0.

- The heat generation in the MPC is assumed to be uniform in each horizontal plane, but to vary in the axial direction to correspond to the axial power distribution listed in Chapter 2.

- The most disadvantageously placed cask (i.e., the one subjected to maximum radiative blockage), is modeled.

- The bottom surface of the overpack, in contact with the ISFSI pad, rejects heat through the pad to the constant temperature (77°F) earth below. For some scenarios, the bottom surface of the overpack is conservatively assumed to be adiabatic.

The finite-volume model constructed in this manner will produce an axisymmetric temperature distribution. The peak temperature will occur at the centerline and is expected to be above the axial location of peak heat generation. As will be shown in Subsection 4.4.2, the results of the finite-volume solution bear out these observations.

The HI-STORM 100 System is evaluated for two fuel storage scenarios. In one scenario, designated as uniform loading, every basket cell is assumed to be occupied with fuel producing heat at the maximum rate. Storage of moderate burnup and high burnup fuels are analyzed for this loading scenario. In another scenario, denoted as regionalized loading, a two-region fuel loading configuration is stipulated. The two regions are defined as an inner region (for storing hot fuel) and an outer region with low decay heat fuel physically enveloping the inner region. This scenario is depicted in Figure 4.4.25. The inner region is shown populated with fuel having a heat load of q_1 and post-core decay time (PCDT) or age τ, and the outer region with fuel of heat load q_2 and age τ_2, where $q_1 > q_2$. For conservatism the outer region fuel permissible cladding temperature (T_2) is assumed to be that of old fuel ($\tau = 15$ years). By ensuring that the interface boundary temperature is less than or equal to T_2 ensures that fuel in the outer region is below permissible temperatures for any fuel age. To permit hot fuel storage in the inner region, a uniform low decay heat rate is stipulated for the outer region fuel. The maximum allowable heat load for inner region fuel (q_1), then, is a function of fuel age-dependent permissible temperature set forth in Table 4.3.7 and Appendix 4.A for moderate and high burnup fuels, respectively. For the regionalized loading scenario, the most restrictive of the two burnups dependent permissible temperature limits is used in the thermal evaluation. In the HI-STORM 100 System, four central locations in the MPC-24 and MPC-24E, twelve inner cells in MPC-32 and 32 in MPC-68 are designated as inner region locations in the regionalized fuel-loading scenario. Results of thermal evaluations for both scenarios are present in Subsection 4.4.2.

4.4.1.1.10 Effect of Fuel Cladding Crud Resistance

In this subsection, a conservatively bounding estimate of temperature drop across a crud film adhering to a fuel rod during dry storage conditions is determined. The evaluation is performed for a BWR fuel assembly based on an upper bound crud thickness obtained from the PNL-4835 report ([4.3.2], Table 3). The crud present on the fuel assemblies is predominately iron oxide mixed with small quantities of other metals such as cobalt, nickel, chromium, etc. Consequently, the effective conductivity of the crud mixture is expected to be in the range of typical metal alloys. Metals have thermal conductivities several orders of magnitude larger than that of helium. In the interest of

extreme conservatism, however, a film of helium with the same thickness replaces the crud layer. The calculation is performed in two steps. In the first step, a crud film resistance is determined based on a bounding maximum crud layer thickness replaced with a helium film on the fuel rod surfaces. This is followed by a peak local cladding heat flux calculation for the GE 7×7 array fuel assembly postulated to emit a conservatively bounding decay heat equal to 0.5kW. The temperature drop across the crud film obtained as a product of the heat flux and crud resistance terms is determined to be less than 0.1°F. The calculations are presented below.

Bounding Crud Thickness(s) = 130μm (4.26×10^{-4} ft) (PNL-4835)
Crud Conductivity (K) = 0.1 Btu/ft-hr-°F (conservatively assumed as helium)
GE 7×7 Fuel Assembly:

Rod O.D. = 0.563"
Active Fuel Length = 150"
Heat Transfer Area = $(7\times7)\times(\pi\times0.563)\times(150/144) = 90.3\ ft^2$
Axial Peaking Factor = 1.195 (Burnup distribution Table 2.1.11)
Decay Heat = 500W (conservative assumption)

$$\text{Crud Resistance} = \frac{\delta}{K} = \frac{4.26\times10^{-4}}{0.1} = 4.26\times10^{-3}\,\frac{ft^2\text{-hr-}°F}{Btu}$$

$$\text{Peak Heat Flux} = \frac{(500\times3.417)\ Btu/hr}{90.3\ ft^2}\times1.195$$

$$= 18.92\times1.195 = 22.6\,\frac{Btu}{ft^2\,hr}$$

Temperature drop (ΔT_c) across crud film

$$= 4.26\times10^{-3}\,\frac{ft^2\text{-hr-}°F}{Btu}\times22.6\,\frac{Btu}{ft^2\text{-hr}}$$

$$= 0.096°F$$

(i.e., less than 0.1°F)

Therefore, it is concluded that deposition of crud does not materially change the SNF cladding temperature.

4.4.1.1.11 Thermal Conductivity Calculations with Diluted Backfill Helium

In this subsection, the thermal conductivities of mixtures of the helium backfill gas and the gaseous fission products released from a hypothetical rupture of 10% of the stored fuel rods are evaluated. The gaseous fission products release fractions are stipulated in NUREG-1536. The released gases will mix with the helium backfill gas and reduce its thermal conductivity. These reduced thermal conductivities are applied to determine fuel assembly, and MPC fuel basket and basket periphery effective conductivities for thermal evaluation of the HI-STORM System.

Appendix C of NUREG/CR-0497 [4.4.7] describes a method for calculating the effective thermal conductivity of a mixture of gases. The same method is also described by Rohsenow and Hartnett [4.2.2]. The following expression is provided by both references:

$$k_{mix} = \sum_{i=1}^{n} \left(\frac{k_i x_i}{x_i + \sum_{\substack{j=1 \\ j \neq i}}^{n} \varphi_{ij} x_j} \right)$$

where:

k_{mix} = thermal conductivity of the gas mixture (Btu/hr-ft-°F)
n = number of gases
k_i = thermal conductivity of gas component i (Btu/hr-ft-°F)
x_i = mole fraction of gas component i

In the preceding equation, the term φ_{ij} is given by the following:

$$\varphi_{ij} = \varphi_{ij} \left[1 + 2.41 \frac{(M_i - M_j)(M_i - 0.142 \cdot M_j)}{(M_i + M_j)^2} \right]$$

where M_i and M_j are the molecular weights of gas components i and j, and ϕ_{ij} is:

$$\varphi_{ij} = \frac{\left[1 + \left(\frac{k_i}{k_j} \right)^{\frac{1}{2}} \left(\frac{M_i}{M_j} \right)^{\frac{1}{4}} \right]^2}{2^{\frac{3}{2}} \left(1 + \frac{M_i}{M_j} \right)^{\frac{1}{2}}}$$

Table 4.4.7 presents a summary of the gas mixture thermal conductivity calculations for the MPC-24 and MPC-68 MPC designs containing design basis fuel assemblies.

Having calculated the gas mixture thermal conductivities, the effective thermal conductivities of the

design basis fuel assemblies are calculated using the finite-volume model described in Subsection 4.4.1.1.2. Only the helium gas conductivity is changed, all other modeling assumptions are the same. The fuel assembly effective thermal conductivities with diluted helium are compared to those with undiluted helium in Table 4.4.8. From this table, it is observed that a 10% rod rupture condition has a relatively minor impact on the fuel assembly effective conductivity. Because the fuel regions comprise only a portion of the overall fuel basket thermal conductivity, the 10% rod rupture condition will have an even smaller impact on the basket effective conductivity.

4.4.1.1.12 Effects of Hypothetical Low Fuel Rod Emissivity

The value of emissivity (ε) utilized in this FSAR was selected as 0.8 based on:

i. the recommendation of an EPRI report [4.1.3]

ii. Holtec's prior licensing experience with the HI-STAR 100 System

iii. other vendors' cask licensing experience with the NRC

iv. authoritative literature citations

The table below provides relevant third party information to support the emissivity value utilized in this FSAR.

Source	Reference	Zircaloy Emissivity
EPRI	[4.1.3]	0.8
TN-68 TSAR	Docket 72-1027	0.8
TN-40	Prairie Island Site Specific ISFSI	0.8
TN-32	Docket 72-1021	0.8
Todreas & Mantuefel	[4.4.8]	0.8
DOE SNF Report	[4.4.9]	0.8

The appropriateness of the selected value of ε is further supported by the information provided by PNL-4835 [4.3.2] and NUREG/CR-0497 [4.4.7]. PNL-4835 reports cladding oxidation thickness in U.S. Zircaloy LWR SNF assemblies (20 μm for PWR and 30 μm for BWR fuel). If these oxide thickness values are applied to the mathematical formulas presented for emissivity determination in [4.4.7], then the computed values are slightly higher than our assumed value of 0.8. It should be recognized that the formulas in [4.4.7] include a conservative assumption that depresses the value of computed emissivity, namely, absence of crud. Significant crud layers develop on fuel cladding surfaces during in-core operation. Crud, which is recognized by the above-mentioned NUREG document as having a boosting effect on ε, is completely neglected.

The above discussion provides a reasonable rationale for our selection of 0.8 as the value for ε.

However, to determine the effect of a hypothetical low emissivity of 0.4, an additional thermal analysis adopting this value has been performed. In this analysis, each fuel rod of a fuel assembly is stipulated to have this uniformly low $\varepsilon = 0.4$ and the effective fuel thermal conductivity is recalculated. In the next step, all cells of an MPC basket are assumed to be populated with this low ε fuel that is further assumed to be emitting decay heat at design basis level. The effective conductivity of this basket populated with low ε fuel is recalculated. Using the recalculated fuel basket conductivity, the HI-STORM system temperature field is recomputed. This exercise is performed for the MPC-24 basket because, as explained in the next paragraph, this basket design, which accommodates a fewer number of fuel assemblies (compared to the MPC-68 and MPC-32) has a higher sensitivity to the emissivity parameter. This analysis has determined that the impact of a low ε assumption on the peak cladding temperature is quite small (about 5°C). It is noted that these sensitivity calculations were performed under the completely suppressed helium thermosiphon cooling assumption. Consequently, as the burden of heat dissipation shouldered by radiation heat transfer under this assumption is much greater, the resultant computed sensitivity is a conservative upper bound for the HI-STORM system.

The relatively insignificant increase in the computed peak clad temperature as a result of applying a large penalty in ε (50%) is consistent with the findings in a German Ph.D. dissertation [4.4.11]. Dr. Anton's study consisted of analyzing a cask containing 4 fuel assemblies with a total heat load of 17 kW and helium inside the fuel cavity. For an emissivity of 0.8, the calculated peak cladding temperature was 337°C. In a sensitivity study, wherein the emissivity was varied from 0.7 to 0.9, the temperature changed only by 5°C, i.e. to 342°C and 332°C. Dr. Anton ascribed two reasons for this low impact of emissivity on computed temperatures. Although the radiative heat emission by a surface decreases with lower emissivity, the fraction of heat reflected from other surfaces increases. In other words, the through-assembly heat dissipation by this means increases thereby providing some compensation for the reduced emission. Additionally, the fourth power of temperature dependence of thermal radiation heat transfer reduces the impact of changes in the coefficients on computed temperatures. For storage containers with larger number of fuel assemblies (like the HI-STORM System), an even smaller impact would be expected, since a larger fraction of the heat is dissipated via the basket conduction heat transfer.

4.4.1.1.13 HI-STORM Temperature Field with Low Heat Emitting Fuel

The HI-STORM 100 thermal evaluations for BWR fuel are grouped in two categories of fuel assemblies proposed for storage in the MPC-68. The two groups are classified as Low Heat Emitting (LHE) fuel assemblies and Design Basis (DB) fuel assemblies. The LHE group of fuel assemblies are characterized by low burnup, long cooling time, and short active fuel lengths. Consequently, their heat loads are dwarfed by the DB group of fuel assemblies. The Dresden-1 (6x6 and 8x8), Quad+, and Humboldt Bay (7x7 and 6x6) fuel assemblies are grouped as the LHE fuel. This fuel is evaluated when encased in Damaged Fuel Containers (DFC). As a result of interruption of radiation heat exchange between the fuel assembly and the fuel basket by the DFC boundary, this configuration is bounding for thermal evaluation. In Table 4.4.2, two canister types for encasing LHE fuel are evaluated – a Holtec design and an existing canister in which some of the Dresden-1 fuel is currently stored (Transnuclear D-1 canister). The most resistive LHE fuel assembly (Dresden- 1 8x8) is considered for thermal evaluation (see Table 4.4.2) in a DFC container. The MPC-68 basket

effective conductivity, loaded with the most resistive fuel assembly (encased in a canister) is provided in Table 4.4.3. To this basket, LHE decay heat is applied and a HI-STORM 100 System thermal solution computed. The peak cladding temperature is computed as 513°F, which is substantially below the temperature limit for long cooled fuel (~635°F).

A thoria rod canister designed for holding a maximum of twenty fuel rods arrayed in a 5x4 configuration is currently stored at the Dresden-1 spent fuel pool. The fuel rods were originally constituted as part of an 8x8 fuel assembly and used in the second and third cycle of Dresden-1 operation. The maximum fuel burnup of these rods is quite low (~14,400 MWD/MTU). The thoria rod canister internal design is a honeycomb structure formed from 12-gage stainless steel plates. The rods are loaded in individual square cells. This long cooled, part assembly (18 fuel rods) and very low fuel burnup thoria rod canister renders it a miniscule source of decay heat. The canister all-metal internal honeycomb construction serves as an additional means of heat dissipation in the fuel cell space. In accordance with fuel loading stipulation in the Technical Specifications, long cooled fuel is loaded toward the basket periphery (i.e., away from the hot centrol core of the fuel basket). All these considerations provide ample assurance that these fuel rods will be stored in a benign thermal environment and, therefore, remain protected during long-term storage.

4.4.1.2 Test Model

A detailed analytical model for thermal design of the HI-STORM System was developed using the FLUENT CFD code and the industry standard ANSYS modeling package, as discussed in Subsection 4.4.1.1. As discussed throughout this chapter and specifically in Section 4.4.6, the analysis incorporates significant conservatisms so as to compute bounding fuel cladding temperatures. Furthermore, compliance with specified limits of operation is demonstrated with adequate margins. In view of these considerations, the HI-STORM System thermal design complies with the thermal criteria set forth in the design basis (Sections 2.1 and 2.2) for long-term storage under normal conditions. Additional experimental verification of the thermal design is therefore not required.

4.4.2 Maximum Temperatures

All four MPC-basket designs developed for the HI-STORM System have been analyzed to determine temperature distributions under long-term normal storage conditions, and the results summarized in this subsection. A cross-reference of HI-STORM thermal analyses at other conditions with associated subsection of the FSAR summarizing obtained results is provided in Table 4.4.22. The MPC baskets are considered to be fully loaded with design basis PWR or BWR fuel assemblies, as appropriate. The systems are arranged in an ISFSI array and subjected to design basis normal ambient conditions with insolation.

As discussed in Subsection 4.4.1.1.1, the thermal analysis is performed using a submodeling process where the results of an analysis on an individual component are incorporated into the analysis of a larger set of components. Specifically, the submodeling process yields directly computed fuel temperatures from which fuel basket temperatures are then calculated. This modeling process differs from previous analytical approaches wherein the basket temperatures were evaluated first and then a

basket-to-cladding temperature difference calculation by Wooten-Epstein or other means provided a basis for cladding temperatures. Subsection 4.4.1.1.2 describes the calculation of an effective fuel assembly thermal conductivity for an equivalent homogenous region. It is important to note that the result of this analysis is a function of thermal conductivity versus temperature. This function for fuel thermal conductivity is then input to the fuel basket effective thermal conductivity calculation described in Subsection 4.4.1.1.4. This calculation uses a finite-element methodology, wherein each fuel cell region containing multiple finite-elements has temperature-varying thermal conductivity properties. The resultant temperature-varying fuel basket thermal conductivity computed by this basket-fuel composite model is then input to the fuel basket region of the FLUENT cask model.

Because the FLUENT cask model incorporates the results of the fuel basket submodel, which in turn incorporates the fuel assembly submodel, the peak temperature reported from the FLUENT model is the peak temperature in any component. In a dry storage cask, the hottest components are the fuel assemblies. It should be noted that, because the fuel assembly models described in Subsection 4.4.1.1.2 include the fuel pellets, the FLUENT calculated peak temperatures reported in Tables 4.4.9 and 4.4.10 are actually peak pellet centerline temperatures which bound the peak cladding temperatures, and are therefore conservatively reported as the cladding temperatures.

Applying the radiative blocking factor applicable for the worst case cask location, conservatively bounding axial temperatures at the most heated fuel cladding are shown in Figures 4.4.16 and 4.4.17 for MPC-24 and MPC-68 to depict the thermosiphon effect in PWR and BWR SNF. From these plots, the upward movement of the hot spot is quite evident. As discussed in this chapter, these calculated temperature distributions incorporate many conservatisms. The maximum fuel clad temperatures for zircaloy clad fuel assemblies are listed in Tables 4.4.9, 4.4.10, 4.4.26, and 4.4.27, which also summarize maximum calculated temperatures in different parts of the MPCs and HI-STORM overpack (Table 4.4.36)..

Figures 4.4.19 and 4.4.20, respectively, depict radial temperature distribution in the PWR (MPC-24) and the BWR (MPC-68) at the horizontal plane where maximum fuel cladding temperature occurs. Finally, axial variations of the ventilation air temperatures and that of the inner shell surface are depicted in Figure 4.4.26 for a bounding heat load.

The following additional observations can be derived by inspecting the temperature field obtained from the finite volume analysis:

- The fuel cladding temperatures are in compliance with the temperature limits determined using both the DCCG methodology [4.3.5] and the PNL CSFM methodology [4.3.1].

- The maximum temperature of the basket structural material is within the stipulated design temperature.

- The maximum temperature of the Boral neutron absorber is below the material supplier's recommended limit.

- The maximum temperatures of the MPC pressure boundary materials are well below their respective ASME Code limits.

- The maximum temperatures of concrete are within the NRC's recommended limits [4.4.10] (See Table 4.3.1.)

Noting that the permissible peak cladding temperature is a function of fuel age, parametric peak fuel cladding temperature versus total decay heat load information is computed from the FLUENT thermal model solution. The allowable fuel cladding temperature limits are presented in Section 4.3 for moderate burnup fuel and in Appendix 4.A for high-burnup fuel.

Because the peak clad temperature limits are dependent on burnup and the fuel age at the start of dry storage, the allowable decay heat load is also dependent on these parameters. Tables 4.4.20, , 4.4.21, 4.4.28, and 4.4.29, for the MPC-24 and MPC-68, MPC-32 and MPC-24E, respectively, present the allowable decay heat load as a function of fuel age for moderate burnup fuel. Tables 4.4.32 through 4.4.35 present the results for high burnup fuel. Burnup and cooling-time curves, developed in source-term calculations in Chapter 5 and reported in Chapter 2, are generated from the heat load limits in those tables. It is noted that the burnup and cooling time curves are developed for the most limiting fuel assembly† of each type (PWR and BWR), but are applied to all assemblies of each type. By definition, the limiting fuel assembly emits more heat than any other assembly of its type at a given burnup and cooling time does. Thus, if the limiting fuel assembly meets the allowable clad temperature limit by a certain margin, then the other fuel assemblies of its type with equal burnup and cooling time will meet the clad temperature limit by an even greater margin. The added margin can be quite considerable. For example, the design-basis PWR assembly is the B&W 15×15, which is used to determine Technical Specification limits for burnup in the HI-STORM System. For certain Westinghouse fuel types, the decay heat loads corresponding to these burnup limits will be about 15% less than that of the design-basis assembly. This decay heat over-prediction for other than design-basis assemblies renders the predicted peak temperatures extremely conservative for those assemblies.

For the regionalized loading scenario as depicted in Figure 4.4.25, outer region decay heat limits are stipulated in Table 4.4.30. The inner region heat load limit will be governed by the peak cladding temperature limit for the hot fuel, provided that the interface cladding temperature limit for long cooled fuel is not exceeded. The MPC-32 and MPC-68 heat load limits are determined by analysis to be governed by this requirement. In the MPC-24 and MPC-24E regionalized loading scenarios, the interface cladding temperature limit is reached first for certain fuel cooling times. Thus, the peak cladding temperatures for these MPCs are below their permissible values by a greater margin. The inner region heat load limits are provided in Table 4.4.31.

The calculated temperatures are based on a series of analyses, described previously in this chapter, that incorporate many conservatisms. A list of the significant conservatisms is provided in

† The limiting fuel assembly (also referred to as the design-basis assembly) is defined as that assembly which is the most heat emissive of its type (PWR or BWR) as a given burnup and cooling time.

Subsection 4.4.6. As such, the calculated temperatures are upper bound values that would exceed actual temperatures.

The above observations lead us to conclude that the temperature field in the HI-STORM System with a fully loaded MPC containing design-basis heat emitting SNF complies with all regulatory and industry temperature limits. In other words, the thermal environment in the HI-STORM System will be conducive to long-term safe storage of spent nuclear fuel.

4.4.3 Minimum Temperatures

In Table 2.2.2 of this report, the minimum ambient temperature condition for the HI-STORM storage overpack and MPC is specified to be -40°F. If, conservatively, a zero decay heat load with no solar input is applied to the stored fuel assemblies, then every component of the system at steady state would be at a temperature of -40°F. All HI-STORM storage overpack and MPC materials of construction will satisfactorily perform their intended function in the storage mode at this minimum temperature condition. Structural evaluations in Chapter 3 show the acceptable performance of the overpack and MPC steel and concrete materials at low service temperatures. Criticality and shielding evaluations (Chapters 5 and 6) are unaffected by temperature.

4.4.4 Maximum Internal Pressure

The MPC is initially filled with dry helium after fuel loading and drying prior to installing the MPC closure ring. During normal storage, the gas temperature within the MPC rises to its maximum operating basis temperature as determined based on the thermal analysis methodology described earlier. The gas pressure inside the MPC will also increase with rising temperature. The pressure rise is determined based on the ideal gas law, which states that the absolute pressure of a fixed volume of gas is proportional to its absolute temperature. Tables 4.4.12, 4.4.13, 4.4.24, and 4.4.25 present summaries of the calculations performed to determine the net free volume in the MPC-24, MPC-68, MPC-32, and MPC-24E, respectively.

The MPC maximum gas pressure is considered for a postulated accidental release of fission product gases caused by fuel rod rupture. For these fuel rod rupture conditions, the amounts of each of the release gas constituents in the MPC cavity are summed and the resulting total pressures determined from the Ideal Gas Law. Based on fission gases release fractions (per NUREG 1536 criteria [4.4.10]), net free volume and initial fill gas pressure, the bounding maximum gas pressures with 1% (normal), 10% (off-normal) and 100% (accident condition) rod rupture are given in Table 4.4.14. The maximum gas pressures listed in Table 4.4.14 are all below the MPC internal design pressure listed in Table 2.2.1.

The inclusion of PWR non-fuel hardware (BPRA control elements and thimble plugs) to the PWR baskets influences the MPC internal pressure through two distinct effects. The presence of non-fuel hardware increases the effective basket conductivity, thus enhancing heat dissipation and lowering fuel temperatures as well as the temperature of the gas filling the space between fuel rods. The gas volume displaced by the mass of non-fuel hardware lowers the cavity free volume. These two effects, namely, temperature lowering and free volume reduction, have opposing influence on the MPC

cavity pressure. The first effect lowers gas pressure while the second effect raises it. In the HI-STORM thermal analysis, the computed temperature field (with non-fuel hardware excluded) has been determined to provide a conservatively bounding temperature field for the PWR baskets (MPC-24, MPC-24E, and MPC-32). The MPC cavity free space is computed based on volume displacement by the heaviest fuel (bounding weight) with non-fuel hardware included.

During in-core irradiation of BPRAs, neutron capture by the B-10 isotope in the neutron absorbing material produces helium. Two different forms of the neutron absorbing material are used in BPRAs: Borosilicate glass and B_4C in a refractory solid matrix (At_2O_3). Borosilicate glass (primarily a constituent of Westinghouse BPRAs) is used in the shape of hollow pyrex glass tubes sealed within steel rods and supported on the inside by a thin-walled steel liner. To accommodate helium diffusion from the glass rod into the rod internal space, a relatively high void volume (~40%) is engineered in this type of rod design. The rod internal pressure is thus designed to remain below reactor operation conditions (2,300 psia and approximately 600°F coolant temperature). The B_4C- Al_2O_3 neutron absorber material is principally used in B&W and CE fuel BPRA designs. The relatively low temperature of the poison material in BPRA rods (relative to fuel pellets) favor the entrapment of helium atoms in the solid matrix.

Several BPRA designs are used in PWR fuel that differ in the number, diameter, and length of poison rods. The older Westinghouse fuel (W-14x14 and W-15x15) has used 6, 12, 16, and 20 rods per assembly BPRAs and the later (W-17x17) fuel uses up to 24 rods per BPRA. The BPRA rods in the older fuel are much larger than the later fuel and, therefore, the B-10 isotope inventory in the 20-rod BPRAs bounds the newer W-17x17 fuel. Based on bounding BPRA rods internal pressure, a large hypothetical quantity of helium (7.2 g-moles/BPRA) is assumed to be available for release into the MPC cavity from each fuel assembly in the PWR baskets. The MPC cavity pressures (including helium from BPRAs) are summarized in Table 4.4.14.

4.4.5 Maximum Thermal Stresses

Thermal expansion induced mechanical stresses due to non-uniform temperature distributions are reported in Chapter 3 of this report. Table 4.4.15 provides a summary of HI-STORM System component temperature inputs for structural evaluation. Table 4.4.19 provides a summary of confinement boundary temperatures during normal storage conditions. Structural evaluation in Section 3.4.4 references these temperature results to demonstrate confinement boundary integrity.

4.4.6 Evaluation of System Performance for Normal Conditions of Storage

The HI-STORM System thermal analysis is based on a detailed and complete heat transfer model that conservatively accounts for all modes of heat transfer in various portions of the MPC and overpack. The thermal model incorporates many conservative features that render the results for long-term storage to be extremely conservative:

1. The most severe levels of environmental factors for long-term normal storage, which are an ambient temperature of 80°F and 10CFR71 insolation levels, were coincidentally imposed on the system.

2. A hypothetical rupture of 10% of the stored fuel rods was conservatively considered for determining the thermal conductivity of the diluted helium backfill gas.

3. The most adversely located HI-STORM System in an ISFSI array was considered for analysis.

4. A conservative assessment of thermosiphon effect in the MPC, which is intrinsic to the HI-STORM fuel basket design is included in the thermal analyses.

5. Not Used

6. No credit was considered for contact between fuel assemblies and the MPC basket wall or between the MPC basket and the basket supports. The fuel assemblies and MPC basket were conservatively considered to be in concentric alignment.

7. The MPC is assumed to be loaded with the SNF type which has the maximum equivalent thermal resistance of all fuel types in its category (BWR or PWR), as applicable.

8. The design basis maximum decay heat loads are used for all thermal-hydraulic analyses. For casks loaded with fuel assemblies having decay heat generation rates less than design basis, additional thermal margins of safety will exist. This is assured by defining the burnup limits, as a function of age, for the fuel assemblies based on the bounding (i.e., most heat emissive) fuel assembly types within each class (PWR or BWR). As demonstrated in the source-term calculations described Chapter 5, the B&W 15×15 and GE 7×7 are the governing PWR and BWR fuel assemblies, respectively. For all other fuel types, the heat emission rates at the design-basis burnup levels will be below the design-basis heat emission rate.

9. Not Used

10. The enhancement of heat transfer owing to the so-called "Rayleigh effect" in the basket/MPC interface region, which was included in the analyses underlying the original CoC on the HI-STORM 100 System, is neglected in this revision of the SAR for conservatism.

11. Aluminum heat conduction elements ignored in the thermal analyses.

Temperature distribution results obtained from this highly conservative thermal model show that the maximum fuel cladding temperature limits are met with adequate margins. Expected margins during normal storage will be much greater due to the many conservative assumptions incorporated in the analysis. The long-term impact of decay heat induced temperature levels on the HI-STORM System structural and neutron shielding materials is considered to be negligible. The maximum local MPC basket temperature level is below the recommended limits for structural materials in terms of susceptibility to stress, corrosion and creep-induced degradation. Furthermore, stresses induced due to imposed temperature gradients are within Code limits. Therefore, it is concluded that the HI-STORM System thermal design is in compliance with 10CFR72 requirements.

Table 4.4.1

SUMMARY OF PWR FUEL ASSEMBLY EFFECTIVE THERMAL CONDUCTIVITIES

Fuel	@ 200°F (Btu/ft-hr-°F)	@ 450°F (Btu/ft-hr-°F)	@ 700°F (Btu/ft-hr-°F)
W - 17×17 OFA	0.182	0.277	0.402
W - 17×17 Standard	0.189	0.286	0.413
W - 17×17 Vantage	0.182	0.277	0.402
W - 15×15 Standard	0.191	0.294	0.430
W - 14×14 Standard	0.182	0.284	0.424
W - 14×14 OFA	0.175	0.275	0.413
B&W - 17×17	0.191	0.289	0.416
B&W - 15×15	0.195	0.298	0.436
CE - 16×16	0.183	0.281	0.411
CE - 14×14	0.189	0.293	0.435
HN† - 15×15 SS	0.180	0.265	0.370
W - 14×14 SS	0.170	0.254	0.361
B&W-15x15 Mark B-11	0.187	0.289	0.424
CE-14x14 (MP2)	0.188	0.293	0.434
IP-1 (14x14) SS	0.125	0.197	0.293

† Haddam Neck Plant B&W or Westinghouse stainless steel clad fuel assemblies.

Table 4.4.2

SUMMARY OF BWR FUEL ASSEMBLY EFFECTIVE THERMAL CONDUCTIVITIES

Fuel	@ 200°F (Btu/ft-hr-°F)	@ 450°F (Btu/ft-hr-°F)	@ 700°F (Btu/ft-hr-°F)
Dresden 1 - 8×8†	0.119	0.201	0.319
Dresden 1 - 6×6†	0.126	0.215	0.345
GE - 7×7	0.171	0.286	0.449
GE - 7×7R	0.171	0.286	0.449
GE - 8×8	0.168	0.278	0.433
GE - 8×8R	0.166	0.275	0.430
GE10 - 8×8	0.168	0.280	0.437
GE11 - 9×9	0.167	0.273	0.422
AC††-10×10 SS	0.152	0.222	0.309
Exxon-10×10 SS	0.151	0.221	0.308
Damaged Dresden-1 8×8† (in a Holtec damaged fuel container)	0.107	0.169	0.254
Humboldt Bay-7x7†	0.127	0.215	0.343
Dresden-1 Thin Clad 6x6†	0.124	0.212	0.343
Damaged Dresden-1 8x8 (in TN D-1 canister)†	0.107	0.168	0.252
8x8 Quad+ Westinghouse†	0.164	0.276	0.435

† Cladding temperatures of low heat emitting Dresden (intact and damaged) SNF in the HI-STORM System will be bounded by design basis fuel cladding temperatures. Therefore, these fuel assembly types are excluded from the list of fuel assemblies (zircaloy clad) evaluated to determine the most resistive SNF type.

†† Allis-Chalmers stainless steel clad fuel assemblies.

Table 4.4.3

MPC BASKET EQUIVALENT ISOTROPIC THERMAL CONDUCTIVITY VALUES††

Basket	@200°F (Btu/ft-hr-°F)	@450°F (Btu/ft-hr-°F)	@700°F (Btu/ft-hr-°F)
MPC-24 (Zircaloy Clad Fuel)	1.109	1.495	1.955
MPC-68 (Zircaloy Clad Fuel)	1.111	1.347	1.591
MPC-24 (Stainless Steel Clad Fuel) †	0.897	1.213	1.577(a)
MPC-68 (Stainless Steel Clad Fuel)†	1.070	1.270	1.451(b)
MPC-32 (Zircaloy Clad Fuel)	1.015	1.271	1.546
MPC-32 (Stainless Steel Clad Fuel)†	0.806	0.987	1.161 (c)
MPC-24E (Zircaloy Clad Fuel)	1.216	1.637	2.133
MPC-24E (Stainless Steel Clad fuel)†	0.991	1.351	1.766 (d)

(a) Conductivity is 19% less than corresponding zircaloy fueled basket.
(b) Conductivity is 9% less than corresponding zircaloy fueled basket.
(c) Conductivity is 25% less than corresponding zircaloy fueled basket.
(d) Conductivity is 17% less than corresponding zircaloy fueled basket.

†† The values reported in this table are conservatively understated.
† Evaluated in a damaged fuel canister (conservatively bounding)

Table 4.4.4

CLOSED CAVITY NUSSELT NUMBER RESULTS
FOR HELIUM-FILLED MPC PERIPHERAL VOIDS†

Temperature (°F)	Nusselt Number (PWR Baskets)	Nusselt Number (BWR Basket)
200	3.17	2.41
450	2.56	1.95
700	2.21	1.68

† For conservatism the Rayleigh effect is ignored in the MPC thermal analyses.

Table 4.4.5

SUMMARY OF 10×10 ARRAY TYPE BWR FUEL ASSEMBLY
EFFECTIVE THERMAL CONDUCTIVITIES†

Fuel Assembly	@ 200°F (Btu/ft-hr-°F)	@ 450°F (Btu/ft-hr-°F)	@ 700°F (Btu/ft-hr-°F)
GE-12/14	0.166	0.269	0.412
Atrium-10	0.164	0.266	0.409
SVEA-96	0.164	0.269	0.416

† The conductivities reported in this table are obtained by the simplified method described in the beginning of Subsection 4 4.1.1.2.

Table 4.4.6

COMPARISON OF ARTIUM-10 BWR FUEL ASSEMBLY CONDUCTIVITY† WITH THE BOUNDING†† BWR FUEL ASSEMBLY CONDUCTIVITY

Temperature (°F)	Atrium-10 BWR Assembly		Bounding BWR Assembly	
	(Btu/ft-hr-°F)	(W/m-K)	(Btu/ft-hr-°F)	(W/m-K)
200	0.225	0.389	0.171	0.296
450	0.345	0.597	0.271	0.469
700	0.504	0.872	0.410	0.710

† The reported effective conductivity has been obtained from a rigorous finite-element model

†† The bounding BWR fuel assembly conductivity applied in the MPC-68 basket thermal analysis.

Table 4.4.7

SUMMARY OF THERMAL CONDUCTIVITY CALCULATIONS
FOR MPC HELIUM DILUTED BY RELEASED ROD GASES

Component Gas	Molecular Weight (g/mole)	Component Gas Mole Fractions and Mixture Conductivity (Btu/hr-ft-°F)	
		MPC-24	MPC-68
MPC Backfill Helium	4	0.951	0.962
Fuel Rod Backfill Helium	4	0.023	5.750×10^{-3}
Rod Tritium	3	1.154×10^{-5}	4.483×10^{-5}
Rod Krypton	85	2.372×10^{-3}	2.905×10^{-3}
Rod Xenon	131	0.024	0.030
Rod Iodine	129	1.019×10^{-3}	1.273×10^{-3}
Mixture of Gases (diluted helium)	N/A	0.088 at 200°F 0.116 at 450°F 0.142 at 700°F	0.086 at 200°F 0.113 at 450°F 0.139 at 700°F

Table 4.4.8

COMPARISON OF COMPONENT THERMAL CONDUCTIVITIES
WITH AND WITHOUT BACKFILL HELIUM DILUTION

	@ 200°F (Btu/hr-ft-°F)	**@ 450°F (Btu/hr-ft-°F)**	**@ 700°F (Btu/hr-ft-°F)**
GE-11 9×9 Fuel Assembly with Undiluted Helium	0.171	0.271	0.410
GE-11 9×9 Fuel Assembly with Diluted Helium	0.158	0.254	0.385
W 17×17 OFA Fuel Assembly with Undiluted Helium	0.257	0.406	0.604
W 17×17 OFA Fuel Assembly with Diluted Helium	0.213	0.347	0.537

Table 4.4.9

HI-STORM† SYSTEM LONG-TERM NORMAL STORAGE MAXIMUM TEMPERATURES (MPC-24 BASKET)

Component	Normal Condition Temp. (°F)	Long-Term Temperature Limit (°F)
Fuel Cladding	691	787††
MPC Basket	650	725†††
Basket Periphery	486	725†††
MPC Outer Shell	344	450

† Bounding overpack temperatures are provided in Table 4.4.36.

†† The temperature limit is in accordance with DCCG (gross rupture) criteria. Permissible peak cladding temperature is 691°F (PNL Criteria).

††† The ASME Code allowable temperature of the fuel basket Alloy X materials is 800°F. This lower temperature limit is imposed to add additional conservatism to the analysis of the HI-STORM System.

Table 4.4.10

HI-STORM† SYSTEM LONG-TERM NORMAL STORAGE MAXIMUM TEMPERATURES (MPC-68 BASKET)

Component	Normal Condition Temp. (°F)	Long-Term Temperature Limit (°F)
Fuel Cladding	740	824††
MPC Basket	720	725†††
Basket Periphery	501	725†††
MPC Outer Shell	347	450

† Bounding overpack temperatures are provided in Table 4.4.36.

†† The temperature limit is in accordance with DCCG (gross rupture) criteria. Permissible cladding temperature is 742°F (PNL criteria).

††† The ASME Code allowable temperature of the fuel basket Alloy X materials is 800°F. This lower temperature limit is imposed to add additional conservatism to the analysis of the HI-STORM System.

Table 4.4.11

INTENTIONALLY DELETED

Table 4.4.12

SUMMARY OF MPC-24 FREE VOLUME CALCULATIONS

Item	Volume (ft^3)
Cavity Volume	367.9
Basket Metal Volume	39.7
Bounding Fuel Assemblies Volume	78.8
Basket Supports and Fuel Spacers Volume	6.1
Aluminum Conduction Elements	5.9†
Net Free Volume	237.5 (6,724 liters)

† Bounding 1,000 lbs weight assumed.

Table 4.4.13

SUMMARY OF MPC-68 FREE VOLUME CALCULATIONS

Item	Volume (ft^3)
Cavity Volume	367.3
Basket Metal Volume	34.8
Bounding Fuel Assemblies Volume	93.0
Basket Supports and Fuel Spacers Volume	11.3
Aluminum Conduction Elements	5.9†
Net Free Volume	222.3 (6,294 liters)

† Bounding 1,000 lbs weight assumed.

Table 4.4.14
SUMMARY OF MPC CONFINEMENT BOUNDARY PRESSURES†
FOR LONG-TERM STORAGE

Condition	Pressure (psig)
MPC-24:	
Initial backfill (at 70°F)	31.3
Normal condition	66.4
With 1% rods rupture	66.1
With 10% rods rupture	72.2
With 100% rods rupture	132.5
MPC-68:	
Initial backfill (at 70°F)	31.3
Normal condition	67.1
With 1% rods rupture	67.5
With 10% rods rupture	71.1
With 100% rods rupture	107.6
MPC-32:	
Initial backfill (at 70°F)	31.3
Normal Condition	65.6
With 1% rods rupture	66.5
With 10% rods rupture	75.0
With 100% rods rupture	160.1
MPC-24E:	
Initial backfill (at 70°F)	31.3
Normal Condition	65.8
With 1% rods rupture	66.4
With 10% rods rupture	72.5
With 100% rods rupture	133.5

† Per NUREG-1536, pressure analyses with ruptured fuel rods (including BPRA rods for PWR fuel) is performed with release of 100% of the ruptured fuel rod fill gas and 30% of the significant radioactive gaseous fission products.

Table 4.4.15

SUMMARY OF HI-STORM SYSTEM COMPONENT TEMPERATURES
FOR LONG-TERM STORAGE (°F)

Location	MPC-24	MPC-68	MPC-32	MPC-24E
MPC Basket Top:				
Basket periphery	485	501	496	488
MPC shell	344	348	351	346
Overpack Inner Shell	199	199	199	199
Overpack Outer Shell	124	124	124	124
MPC Basket Bottom:				
Basket periphery	281	280	290	284
MPC shell	256	258	261	258
Overpack Inner Shell	106	106	106	106
Overpack Outer Shell	107	107	107	107

Table 4.4.16

INTENTIONALLY DELETED

Table 4.4.17

INTENTIONALLY DELETED

Table 4.4.18

INTENTIONALLY DELETED

Table 4.4.19

SUMMARY OF MPC CONFINEMENT BOUNDARY TEMPERATURE DISTRIBUTIONS

Location	MPC-24 (°F)	MPC-68 (°F)	MPC-32 (°F)	MPC-24E (°F)
MPC Lid Inside Surface at Centerline	463	502	487	462
MPC Lid Outside Surface at Centerline	427	454	447	425
MPC Lid Inside Surface at Periphery	371	381	383	372
MPC Lid Outside Surface at Periphery	360	375	372	358
MPC Baseplate Inside Surface at Centerline	207	209	214	209
MPC Baseplate Outside Surface at Centerline	200	203	208	202
MPC Baseplate Inside Surface at Periphery	243	246	249	245
MPC Baseplate Outside Surface at Periphery	194	196	199	195

Table 4.4.20

MPC-24 DESIGN-BASIS MAXIMUM HEAT LOAD†
VERSUS FUEL AGE AT LOADING (MODERATE BURNUP)

Fuel Age At Loading (years)	Permissible Heat Load (kW)
5	27.77
6	26.96
7	24.74
10	24.23
15	23.66

† The cask heat load limits (Q_τ) presented in this table pertain to loading the MPC with uniformly aged fuel assemblies emitting heat at the design basis maximum rate (q_τ), where "τ" is the age of the fuel at the start of dry storage. For a cask loaded with a mix of fuel ages, the cask heat load limit shall be the sum of the individual assembly decay heat limits (as a function of τ) as specified in the Appendix B to COC 1014.

Table 4.4.21

MPC-68 DESIGN-BASIS MAXIMUM HEAT LOAD†
VERSUS FUEL AGE AT LOADING (MODERATE BURNUP)

Fuel Age At Loading (years)	Permissible Heat Load (kW)
5	28.19
6	26.81
7	24.71
10	24.18
15	23.60

† The cask heat load limits (Q_τ) presented in this table pertain to loading the MPC with uniformly aged fuel assemblies emitting heat at the design basis maximum rate (q_τ), where "τ" is the age of fuel at the start of dry storage. For a cask loaded with a mix of fuel ages, the cask heat load limit shall be the sum of the individual assembly decay heat limits (as a function of fuel age) as specified in the Appendix B to COC 1014.

Table 4.4.22
MATRIX OF HI-STORM SYSTEM THERMAL EVALUATIONS

Scenario	Description	Ultimate Heat Sink	Analysis Type	Principal Input Parameters	Results in FSAR Subsection
1	Long Term Normal	Ambient	SS	N_T, Q_D, ST, SC, I_O	4.4.2
2	Off-Normal Environment	Ambient	SS(B)	O_T, Q_D, ST, SC, I_O	11.1.2
3	Extreme Environment	Ambient	SS(B)	E_T, Q_D, ST, SC, I_O	11.2.15
4	Partial Ducts Blockage	Ambient	SS(B)	N_T, Q_D, ST, SC, $I_{1/4}$	11.1.4
5	Ducts Blockage Accident	Overpack	TA	N_T, Q_D, ST, SC, I_C	11.2.13
6	Fire Accident	Overpack	TA	Q_D, F	11.2.4
7	Tip Over Accident	Overpack	AH	Q_D	11.2.3
8	Debris Burial Accident	Overpack	AH	Q_D	11.2.14

Legend:

N_T - Maximum Annual Average (Normal) Temperature (80°F)
O_T - Off-Normal Temperature (100°F)
E_T - Extreme Hot Temperature (125°F)
Q_D - Design Basis Maximum Heat Load
SS - Steady State
SS(B) - Bounding Steady State
TA - Transient Analysis
AH - Adiabatic Heating

I_O - All Inlet Ducts Open
$I_{1/2}$ - Half of Inlet Ducts Open
$I_{1/4}$ - Quarter of Inlet Ducts Open
I_C - All Inlet Ducts Closed

ST - Insolation Heating (Top)
SC - Insolation Heating (Curved)
F - Fire Heating (1475°F)

Table 4.4.23

PLANT SPECIFIC BWR FUEL TYPES EFFECTIVE CONDUCTIVITY†

Fuel	@200°C [Btu/ft-hr-°F]	@450°F [Btu/ft-hr-°F]	@700°F [Btu/ft-hr-°F]
Oyster Creek (7x7)	0.161	0.269	0.422
Oyster Creek (8x8)	0.162	0.266	0.413
TVA Browns Ferry (8x8)	0.160	0.264	0.411
SPC-5 (9x9)	0.149	0.245	0.380
ANF 8x8	0.167	0.277	0.433
ANF-9X (9x9)	0.165	0.272	0.423

† The conductivities reported in this table are obtained by a simplified analytical method in Subsection 4.4.1.1.2

Table 4.4.24

SUMMARY OF MPC-32 FREE VOLUME CALCULATIONS

Item	Volume (ft^3)
Cavity Volume	367.9
Basket Metal Volume	27.4
Bounding Free Assemblies Volume	105.0
Basket Supports and Fuel Spacers Volume	9.0
Optional Aluminum Conduction Elements	5.9
Net Free Volume	220.6 (6,247 liters)

Table 4.4.25

SUMMARYOF MPC-24E FREE VOLUME CALCULATIONS

Item	Volume (ft^3)
Cavity Volume	367.9
Basket Metal Volume	51.2
Bounding Fuel Assemblies Volume	78.8
Basket Supports and Fuel Spacers Volume	6.1
Optional Aluminum Conduction Elements	5.9
Net Free Volume	225.9 (6,398 liters)

Table 4.4.26

HI-STORM† SYSTEM LONG-TERM NORMAL STORAGE MAXIMUM TEMPERATURES (MPC-32 BASKET)

Component	Normal Condition Temp. (°F)	Long-Term Temperature Limit (°F)
Fuel Cladding	691	787††
MPC Basket	660	725†††
Basket Periphery	496	725†††
MPC Outer Shell	351	450

† Bounding overpack temperatures are provided in Table 4.4.36.

†† The temperature limit is in accordance with DCCG (gross rupture) criteria. Permissible peak cladding temperature is 691°F PNL Criteria).

††† The ASME Code allowable temperature of the fuel basket Alloy X materials is 800°F. This lower temperature limit is imposed to add additional conservatism in the analysis of the HI-STORM Systems.

Table 4.4.27

HI-STORM† SYSTEM LONG-TERM NORMAL STORAGE MAXIMUM TEMPERATURES (MPC-24E BASKET)

Component	Normal Condition Temp. (°F)	Long-Term Temperature Limit (°F)
Fuel Cladding	691	787††
MPC Basket	650	725†††
Basket Periphery	492	725†††
MPC Outer Shell	347	450

† Bounding overpack temperatures are provided in Table 4.4.36.

†† The temperature limit is in accordance with DCCG (gross rupture) criteria. Permissible peak cladding temperature is 691°F (PNL Criteria).

††† The ASME Code allowable temperature of the fuel basket Alloy X materials is 800°F. This lower temperature limit is imposed to add additional conservatism to the analysis of the HI-STORM System.

Table 4.4.28

MPC-32 DESIGN BASIS MAXIMUM HEAT LOAD† VERSUS FUEL AGE AT LOADING (MODERATE BURNUP)

Fuel Age at Loading (years)	Permissible Heat Load (kW)
5	28.74
6	27.95
7	25.79
10	25.26
15	24.68

† The cask heat load limits (Q_τ) presented in this table pertain to loading the MPC with uniformly aged fuel assemblies emitting heat at the design basis maximum rate (q_τ) where "τ" is the age of fuel at the start of dry storage For a cask loaded with a mix of fuel ages, the cask heat load limit shall be the sum of the individual assembly decay heat limits (as a function of fuel age) as specified in the Appendix B to CoC 1014.

4.5 THERMAL EVALUATION FOR NORMAL HANDLING AND ONSITE TRANSPORT

Prior to placement in a HI-STORM overpack, an MPC must be loaded with fuel, outfitted with closures, dewatered, vacuum dried, backfilled with helium and transported to the HI-STORM module. In the unlikely event that the fuel needs to be returned to the spent fuel pool, these steps must be performed in reverse. Finally, if required, transfer of a loaded MPC between HI-STORM overpacks or between a HI-STAR transport overpack and a HI-STORM storage overpack must be carried out in an assuredly safe manner. All of the above operations are short duration events that would likely occur no more than once or twice for an individual MPC.

The device central to all of the above operations is the HI-TRAC transfer cask that, as stated in Chapter 1, is available in two anatomically identical weight ratings (100- and 125-ton). The HI-TRAC transfer cask is a short-term host for the MPC; therefore it is necessary to establish that, during all thermally challenging operation events involving either the 100-ton or 125-ton HI-TRAC, the permissible temperature limits presented in Section 4.3 are not exceeded. The following discrete thermal scenarios, all of short duration, involving the HI-TRAC transfer cask have been identified as warranting thermal analysis.

i. Normal Onsite Transport
ii. MPC Cavity Vacuum Drying
iii. Post-Loading Wet Transfer Operations
iv. MPC Cooldown and Reflood for Unloading Operations

The above listed conditions are described and evaluated in the following subsections. Subsection 4.5.1 describes the individual analytical models used to evaluate these conditions. Due to the simplicity of the conservative evaluation of wet transfer operations, Subsection 4.5.1.1.5 includes both the analysis model and analysis results discussions. The maximum temperature analyses for onsite transport and vacuum drying are discussed in Subsection 4.5.2. Subsections 4.5.3, 4.5.4 and 4.5.5, respectively, discuss minimum temperature, MPC maximum internal pressure and thermal data for stress analyses during onsite transport.

4.5.1 Thermal Model

The HI-TRAC transfer cask is used to load and unload the HI-STORM concrete storage overpack, including onsite transport of the MPCs from the loading facility to an ISFSI pad. Section views of the HI-TRAC have been presented in Chapter 1. Within a loaded HI-TRAC, heat generated in the MPC is transported from the contained fuel assemblies to the MPC shell in the manner described in Section 4.4. From the outer surface of the MPC to the ambient air, heat is transported by a combination of conduction, thermal radiation and natural convection. It has been demonstrated in Section 4.3 that from a thermal standpoint, storage of stainless steel clad fuel assemblies is bounded by storage of zircaloy clad fuel assemblies. Thus, only zircaloy clad fuel assemblies shall be considered in the HI-TRAC thermal performance evaluations. Analytical modeling details of all the various thermal transport mechanisms are provided in the following subsection.

Two HI-TRAC transfer cask designs, namely, the 125-ton and the 100-ton versions, are developed for onsite handling and transport, as discussed in Chapter 1. The two designs are principally different in terms of lead thickness and the thickness of radial connectors in the water jacket region. The analytical model developed for HI-TRAC thermal characterization conservatively accounts for these differences by applying the higher shell thickness and thinner radial connectors' thickness to the model. In this manner, the HI-TRAC overpack resistance to heat transfer is overestimated, resulting in higher predicted MPC internals and fuel cladding temperature levels.

4.5.1.1 Analytical Model

From the outer surface of the MPC to the ambient atmosphere, heat is transported within HI-TRAC through multiple concentric layers of air, steel and shielding materials. Heat must be transported across a total of six concentric layers, representing the air gap, the HI-TRAC inner shell, the lead shielding, the HI-TRAC outer shell, the water jacket and the enclosure shell. From the surface of the enclosure shell heat is rejected to the atmosphere by natural convection and radiation.

A small diametral air gap exists between the outer surface of the MPC and the inner surface of the HI-TRAC overpack. Heat is transported across this gap by the parallel mechanisms of conduction and thermal radiation. Assuming that the MPC is centered and does not contact the transfer overpack walls conservatively minimizes heat transport across this gap. Additionally, thermal expansion that would minimize the gap is conservatively neglected. Heat is transported through the cylindrical wall of the HI-TRAC transfer overpack by conduction through successive layers of steel, lead and steel. A water jacket, which provides neutron shielding for the HI-TRAC overpack, surrounds the cylindrical steel wall. The water jacket is composed of carbon steel channels with welded, connecting enclosure plates. Conduction heat transfer occurs through both the water cavities and the channels. While the water jacket channels are sufficiently large for natural convection loops to form, this mechanism is conservatively neglected. Heat is passively rejected to the ambient from the outer surface of the HI-TRAC transfer overpack by natural convection and thermal radiation.

In the vertical position, the bottom face of the HI-TRAC is in contact with a supporting surface. This face is conservatively modeled as an insulated surface. Because the HI-TRAC is not used for long-term storage in an array, radiative blocking does not need to be considered. The HI-TRAC top lid is modeled as a surface with convection, radiative heat exchange with air and a constant maximum incident solar heat flux load. Insolation on cylindrical surfaces is conservatively based on 12-hour levels prescribed in 10CFR71 averaged on a 24-hour basis. Concise descriptions of these models are given below.

4.5.1.1.1 Effective Thermal Conductivity of Water Jacket

The 125-ton HI-TRAC water jacket is composed of fourteen formed channels equispaced along the circumference of the HI-TRAC and welded along their length to the HI-TRAC outer shell. Enclosure plates are welded to these channels, creating twenty-eight water compartments. The 100-ton HI-TRAC water jacket has 15 formed channels and enclosure plates creating thirty compartments. Holes in the channel legs connect all the individual compartments in the water jacket. Thus, the annular region between the HI-TRAC outer shell and the enclosure shell can be considered as an array of steel ribs and water spaces.

The effective radial thermal conductivity of this array of steel ribs and water spaces is determined by combining the heat transfer resistance of individual components in a parallel network. A bounding calculation is assured by using the minimum number of channels and channel thickness as input values. The thermal conductivity of the parallel steel ribs and water spaces is given by the following formula:

$$K_{ne} = \frac{K_r N_r t_r \ln\left(\frac{r_o}{r_i}\right)}{2\pi L_R} + \frac{K_w N_r t_w \ln\left(\frac{r_o}{r_i}\right)}{2\pi L_R}$$

where:

K_{ne} = effective radial thermal conductivity of water jacket
r_i = inner radius of water spaces
r_o = outer radius of water spaces
K_r = thermal conductivity of carbon steel ribs
N_r = minimum number of channel legs (equal to number of water spaces)
t_r = minimum (nominal) rib thickness (lower of 125-ton and 100-ton designs)
L_R = effective radial heat transport length through water spaces
K_w = thermal conductivity of water
t_w = water space width (between two carbon steel ribs)

Figure 4.5.1 depicts the resistance network to combine the resistances to determine an effective conductivity of the water jacket. The effective thermal conductivity is computed in the manner of the foregoing, and is provided in Table 4.5.1.

4.5.1.1.2 Heat Rejection from Overpack Exterior Surfaces

The following relationship for the surface heat flux from the outer surface of an isolated cask to the environment applied to the thermal model:

$$q_s = 0.19\,(T_s - T_A)^{4/3} + 0.1714\varepsilon\left[\left(\frac{T_s + 460}{100}\right)^4 - \left(\frac{T_A + 460}{100}\right)^4\right]$$

where:

T_S = cask surface temperatures (°F)
T_A = ambient atmospheric temperature (°F)
q_s = surface heat flux (Btu/ft^2×hr)
ε = surface emissivity

The second term in this equation the Stefan-Boltzmann formula for thermal radiation from an exposed surface to ambient. The first term is the natural convection heat transfer correlation recommended by Jacob and Hawkins [4.2.9]. This correlation is appropriate for turbulent natural convection from vertical surfaces, such as the vertical overpack wall. Although the ambient air is conservatively assumed to be quiescent, the natural convection is nevertheless turbulent.

Turbulent natural convection correlations are suitable for use when the product of the Grashof and Prandtl (Gr×Pr) numbers exceeds 10^9. This product can be expressed as $L^3 \times \Delta T \times Z$, where L is the characteristic length, ΔT is the surface-to-ambient temperature difference, and Z is a function of the surface temperature. The characteristic length of a vertically oriented HI-TRAC is its height of approximately 17 feet. The value of Z, conservatively taken at a surface temperature of 340°F, is 2.6×10^5. Solving for the value of ΔT that satisfies the equivalence $L^3 \times \Delta T \times Z = 10^9$ yields ΔT = 0.78°F. For a horizontally oriented HI-TRAC the characteristic length is the diameter of approximately 7.6 feet (minimum of 100- and 125-ton designs), yielding ΔT = 8.76°F. The natural convection will be turbulent, therefore, provided the surface to air temperature difference is greater than or equal to 0.78°F for a vertical orientation and 8.76°F for a horizontal orientation.

4.5.1.1.3 Determination of Solar Heat Input

As discussed in Section 4.4.1.1.8, the intensity of solar radiation incident on an exposed surface depends on a number of time varying terms. A twelve-hour averaged insolation level is prescribed in 10CFR71 for curved surfaces. The HI-TRAC cask, however, possesses a considerable thermal inertia. This large thermal inertia precludes the HI-TRAC from reaching a steady-state thermal condition during a twelve-hour period. Thus, it is considered appropriate to use the 24-hour averaged insolation level.

4.5.1.1.4 MPC Temperatures During Moisture RemovalOperations

4.5.1.1.4.1 Vacuum Drying

The initial loading of SNF in the MPC requires that the water within the MPC be drained and replaced with helium. For MPCs containing moderate burnup fuel assemblies only, this operation may be carried out using the conventional vacuum drying approach. In this method, removal of the last traces of residual moisture from the MPC cavity is accomplished by evacuating the MPC for a short time after draining the MPC. As stipulated in the Technical Specifications, vacuum drying may not be performed on MPCs containing high burnup fuel assemblies. High burnup fuel drying is

performed by a forced flow helium drying process as described in Section 4.5.1.1.4.2 and Appendix 2.B.

Prior to the start of the MPC draining operation, both the HI-TRAC annulus and the MPC are full of water. The presence of water in the MPC ensures that the fuel cladding temperatures are lower than design basis limits by large margins. As the heat generating active fuel length is uncovered during the draining operation, the fuel and basket mass will undergo a gradual heat up from the initially cold conditions when the heated surfaces were submerged under water.

The vacuum condition effective fuel assembly conductivity is determined by procedures discussed earlier (Subsection 4.4.1.1.2) after setting the thermal conductivity of the gaseous medium to a small fraction (one part in one thousand) of helium conductivity. The MPC basket cross sectional effective conductivity is determined for vacuum conditions according to the procedure discussed in 4.4.1.1.4. Basket periphery-to-MPC shell heat transfer occurs through conduction and radiation.

For total decay heat loads up to and including 20.88 kW for the MPC-24 and 21.52 kW for the MPC-68, vacuum drying of the MPC is performed with the annular gap between the MPC and the HI-TRAC filled with water. The presence of water in this annular gap will maintain the MPC shell temperature approximately equal to the saturation temperature of the annulus water. Thus, the thermal analysis of the MPC during vacuum drying for these conditions is performed with cooling of the MPC shell with water at a bounding maximum temperature of 232°F.

For higher total decay heat loads in the MPC-24 and MPC-68 or for any decay heat load in an MPC-24E or MPC-32, vacuum drying of the MPC is performed with the annular gap between the MPC and the HI-TRAC continuously flushed with water. The water movement in this annular gap will maintain the MPC shell temperature at about the temperature of flowing water. Thus, the thermal analysis of the MPC during vacuum drying for these conditions is performed with cooling of the MPC shell with water at a bounding maximum temperature of 125°F.

An axisymmetric FLUENT thermal model of the MPC is constructed, employing the MPC in-plane conductivity as an isotropic fuel basket conductivity (i.e. conductivity in the the basket radial and axial directions is equal), to determine peak cladding temperature at design basis heat loads. To avoid excessive conservatism in the computed FLUENT solution, partial recognition for higher axial heat dissipation is adopted in the peak cladding calculations. The boundary conditions applied to this evaluation are:

i. A bounding steady-state analysis is performed with the MPC decay heat load set equal to the largest design-basis decay heat load. As discussed above, there are two different ranges for the MPC-24 and MPC-68 designs.

ii. The entire outer surface of the MPC shell is postulated to be at a bounding maximum temperature of 232°F or 125°F, as discussed above.

iii. The top and bottom surfaces of the MPC are adiabatic.

Results of vacuum condition analyses are provided in Subsection 4.5.2.2.

4.5.1.1.4.2 Forced Helium Recirculation

To reduce moisture to trace levelsin the MPC using a Forced Helium Dehydration (FHD) system, a conventional, closed loop dehumidification system consisting of a condenser, a demoisturizer, a compressor, and a pre-heater is utilized to extract moisture from the MPC cavity through repeated displacement of its contained helium, accompanied by vigorous flow turbulation. A vapor pressure of 3 torr or less is assured by verifying that the helium temperature exiting the demoisturizer is maintained at or below the psychrometric threshold of 21°F for a minimum of 30 minutes. See Appendix 2.B for detailed discussionof the design criteria and operation of the FHD system.

The FHD system provides concurrent fuel cooling during the moisture removal process through forced convective heat transfer. The attendant forced convection-aided heat transfer occurring during operation of the FHD system ensures that the fuel cladding temperature will remain below the applicable peak cladding temperature limit for normal conditions of storage, which is well below the high burnup cladding temperature limit 752°F (400°C) for all combinations of SNF type, burnup, decay heat, and cooling time. Because the FHD operation induces a state of forced convection heat transfer in the MPC,(in contrast to the quiescent mode of natural convection in long term storage), it is readily concluded that the peak fuel cladding temperature under the latter condition will be greater than that during the FHD operation phase. In the event that the FHD system malfunctions, the forced convection state will degenerate to natural convection, which corresponds to the conditions of normal storage. As a result, the peak fuel cladding temperatures will approximate the values reached during normal storage as described elsewhere in this chapter.

4.5.1.1.5 Maximum Time Limit During Wet Transfer Operations

In accordance with NUREG-1536, water inside the MPC cavity during wet transfer operations is not permitted to boil. Consequently, uncontrolled pressures in the de-watering, purging, and recharging system that may result from two-phase conditions are completely avoided. This requirement is accomplished by imposing a limit on the maximum allowable time duration for fuel to be submerged in water after a loaded HI-TRAC cask is removed from the pool and prior to the start of vacuum drying operations.

When the HI-TRAC transfer cask and the loaded MPC under water-flooded conditions are removed from the pool, the combined water, fuel mass, MPC, and HI-TRAC metal will absorb the decay heat emitted by the fuel assemblies. This results in a slow temperature rise of the entire system with time, starting from an initial temperature of the contents. The rate of temperature rise is limited by the thermal inertia of the HI-TRAC system. To enable a bounding heat-up rate determination for the HI-TRAC system, the following conservative assumptions are imposed:

i. Heat loss by natural convection and radiation from the exposed HI-TRAC surfaces to the pool building ambient air is neglected (i.e., an adiabatic temperature rise calculation is performed).

ii. Design-basis maximum decay heat input from the loaded fuel assemblies is imposed on the HI-TRAC transfer cask.

iii. The smaller of the two (i.e., 100-ton and 125-ton) HI-TRAC transfer cask designs is credited in the analysis. The 100-ton design has a significantly smaller quantity of metal mass, which will result in a higher rate of temperature rise.

iv. The smallest of the minimum MPC cavity-free volumes among the two MPC types is considered for flooded water mass determination.

v. Only fifty percent of the water mass in the MPC cavity is credited towards water thermal inertia evaluation.

Table 4.5.5 summarizes the weights and thermal inertias of several components in the loaded HI-TRAC transfer cask. The rate of temperature rise of the HI-TRAC transfer cask and contents during an adiabatic heat-up is governed by the following equation:

$$\frac{dT}{dt} = \frac{Q}{C_h}$$

where:

Q = decay heat load (Btu/hr) [Design Basis maximum 28.74 kW = 98,205 Btu/hr]
C_h = combined thermal inertia of the loaded HI-TRAC transfer cask (Btu/°F)
T = temperature of the contents (°F)
t = time after HI-TRAC transfer cask is removed from the pool (hr)

A bounding heat-up rate for the HI-TRAC transfer cask contents is determined to be equal to 3.77 °F/hr. From this adiabatic rate of temperature rise estimate, the maximum allowable time duration (t_{max}) for fuel to be submerged in water is determined as follows:

$$t_{max} = \frac{T_{boil} - T_{initial}}{(dT/dt)}$$

where:

T_{boil} = boiling temperature of water (equal to 212°F at the water surface in the MPC cavity)
$T_{initial}$ = initial temperature of the HI-TRAC contents when the transfer cask is removed from the pool

Table 4.5.6 provides a summary of t_{max} at several representative HI-TRAC contents starting temperature.

As set forth in the HI-STORM operating procedures, in the unlikely event that the maximum allowable time provided in Table 4.5.6 is found to be insufficient to complete all wet transfer operations, a forced water circulation shall be initiated and maintained to remove the decay heat from the MPC cavity. In this case, relatively cooler water will enter via the MPC lid drain port connection and heated water will exit from the vent port. The minimum water flow rate required to maintain the MPC cavity water temperature below boiling with an adequate subcooling margin is determined as follows:

$$M_W = \frac{Q}{C_{pW}(T_{max} - T_{in})}$$

where:

M_W = minimum water flow rate (lb/hr)
C_{pw} = water heat capacity (Btu/lb-°F)
T_{max} = maximum MPC cavity water mass temperature
T_{in} = temperature of pool water supply to MPC

With the MPC cavity water temperature limited to 150°F, MPC inlet water maximum temperature equal to 125°F and at the design basis maximum heat load, the water flow rate is determined to be 3928 lb/hr (7.9 gpm).

4.5.1.1.6 Cask Cooldown and Reflood Analysis During Fuel Unloading Operation

NUREG-1536 requires an evaluation of cask cooldown and reflood procedures to support fuel unloading from a dry condition. Past industry experience generally supports cooldown of cask internals and fuel from hot storage conditions by direct water quenching. The extremely rapid cooldown rates to which the hot MPC internals and the fuel cladding are subjected during water injection may, however, result in uncontrolled thermal stresses and failure in the structural members. Moreover, water injection results in large amounts of steam generation and unpredictable transient two-phase flow conditions inside the MPC cavity, which may result in overpressurization of the confinement boundary. To avoid potential safety concerns related to rapid cask cooldown by direct water quenching, the HI-STORM MPCs will be cooled in a gradual manner, thereby eliminating thermal shock loads on the MPC internals and fuel cladding.

In the unlikely event that a HI-STORM storage system is required to be unloaded, the MPC will be transported on-site via the HI-TRAC transfer cask back to the fuel handling building. Prior to reflooding the MPC cavity with water[†], a forced flow helium recirculation system with adequate flow capacity shall be operated to remove the decay heat and initiate a slow cask cooldown lasting for several days. The operating procedures in Chapter 8 (Section 8.3) provide a detailed description of the steps involved in the cask unloading. An analytical method that provides a basis for determining

[†] Prior to helium circulation, the HI-TRAC annulus is flooded with water to substantially lower the MPC shell temperature (approximately 100°F). For low decay heat MPCs (~10 kW or less) the annulus cooling is adequate to lower the MPC cavity temperature below the boiling temperature of water.

the required helium flow rate as a function of the desired cooldown time is presented below, to meet the objective of eliminating thermal shock when the MPC cavity is eventually flooded with water.

Under a closed-loop forced helium circulation condition, the helium gas is cooled, via an external chiller, down to 100°F. The chilled helium is then introduced into the MPC cavity, near the MPC baseplate, through the drain line. The helium gas enters the MPC basket from the bottom oversized flow holes and moves upward through the hot fuel assemblies, removing heat and cooling the MPC internals. The heated helium gas exits from the top of the basket and collects in the top plenum, from where it is expelled through the MPC lid vent connection to the helium recirculation and cooling system. The MPC contents bulk average temperature reduction as a function of time is principally dependent upon the rate of helium circulation. The temperature transient is governed by the following heat balance equation:

$$C_h \frac{dT}{dt} = Q_D - m\,C_p\,(T - T_i) - Q_c$$

Initial Condition: $T = T_o$ at $t = 0$

where:

- T = MPC bulk average temperature (°F)
- T_o = initial MPC bulk average temperature in the HI-TRAC transfer cask (equal to 586°F)
- t = time after start of forced circulation (hrs)
- Q_D = decay heat load (Btu/hr)
- (equal to Design Basis maximum 28.74kW (i.e., 98,205 Btu/hr)m = helium circulation rate (lb/hr)
- C_p = helium heat capacity (Btu/lb-°F) (equal to 1.24 Btu/lb-°F)
- Q_c = heat rejection from cask exposed surfaces to ambient (Btu/hr) (conservatively neglected)
- C_h = thermal capacity of the loaded MPC (Btu/°F) (For a bounding upper bound 100,000 lb loaded MPC weight and heat capacity of Alloy X equal to 0.12 Btu/lb-°F, the heat capacity is equal to 12,000 Btu/°F.)
- T_i = MPC helium inlet temperature (°F)

The differential equation is analytically solved, yielding the following expression for time-dependent MPC bulk temperature:

$$T(t) = \left(T_i + \frac{Q_D}{m\,C_p}\right)\left(1 - e^{-\frac{m C_p}{C_h} t}\right) + T_o\, e^{-\frac{m C_p}{C_h} t}$$

This equation is used to determine the minimum helium mass flow rate that would cool the MPC cavity down from initially hot conditions to less than 200°F (i.e., with a subcooling margin for

normal boiling temperature of water† (212°F)). For example, to cool the MPC to less than 200°F in 72 hours using 0°F helium would require a helium mass flow rate of 432 lb/hr (i.e., 647 SCFM).

Once the helium gas circulation has cooled the MPC internals to less than 200°F, water can be injected to the MPC without risk of boiling and the associated thermal stress concerns. Because of the relatively long cooldown period, the thermal stress contribution to the total cladding stress would be negligible, and the total stress would therefore be bounded by the normal (dry) condition. The elimination of boiling eliminates any concern of overpressurization due to steam production.

4.5.1.1.7 Study of Lead-to-Steel Gaps on Predicted Temperatures

Lead, poured between the inner and outer shells, is utilized as a gamma shield material in the HI-TRAC on-site transfer cask designs. Lead shrinks during solidification requiring the specification and implementation of appropriate steps in the lead installation process so that the annular space is free of gaps. Fortunately, the lead pouring process is a mature technology and proven methods to insure that radial gaps do not develop are widely available. This subsection outlines such a method to achieve a zero-gap lead installation in the annular cavity of the HI-TRAC casks.

The 100-ton and 125-ton HI-TRAC designs incorporate 2.5 inch and 4.5 inch annular spaces, respectively, formed between a 3/4-inch thick steel inner shell and a 1-inch thick steel outer shell. The interior steel surfaces are cleaned, sandblasted and fluxed in preparation for the molten lead that will be poured in the annular cavity. The appropriate surface preparation technique is essential to ensure that molten lead sticks to the steel surfaces, which will form a metal to lead bond upon solidification. The molten lead is poured to fill the annular cavity. The molten lead in the immediate vicinity of the steel surfaces, upon cooling by the inner and outer shells, solidifies forming a melt-solid interface. The initial formation of a gap-free interfacial bond between the solidified lead and steel surfaces initiates a process of lead crystallization from the molten pool onto the solid surfaces. Static pressure from the column of molten lead further aids in retaining the solidified lead layer to the steel surfaces. The melt-solid interface growth occurs by freezing of successive layers of molten lead as the heat of fusion is dissipated by the solidified metal and steel structure enclosing it. This growth stops when all the molten lead is used up and the annulus is filled with a solid lead plug. The shop fabrication procedures, being developed in conjunction with the designated manufacturer of the HI-TRAC transfer casks, shall contain detailed step-by-step instructions devised to eliminate the incidence of annular gaps in the lead space of the HI-TRAC.

In the spirit of a defense-in-depth approach, however, a conservatively bounding lead-to-steel gap is assumed herein and the resultant peak cladding temperature under design basis heat load is computed. It is noted that in a non-bonding lead pour scenario, the lead shrinkage resulting from phase transformation related density changes introduces a tendency to form small gaps. This tendency is counteracted by gravity induced slump, which tends to push the heavy mass of lead against the steel surfaces. If the annular molten mass of lead is assumed to contract as a solid, in the

† Certain fuel configurations in PWR MPCs are required to be flooded with borated water, which has a higher boiling temperature. Thus, greater subcooling margins are present in this case

absence of gravity, then a bounding lead-to-steel gap is readily computed from density changes. This calculation is performed for the 125-ton HI-TRAC transfer cask, which has a larger volume of lead and is thus subject to larger volume shrinkage relative to the 100-ton design, and is presented below.

The densities of molten (ρ_l) and solid (ρ_s) lead are given on page 3-96 of Perry's Handbook (6th Edition) as 10,430 kg/m^3 and 11,010 kg/m^3, respectively. The fractional volume contraction during solidification ($\delta v/v$) is calculated as:

$$\frac{\delta v}{v} = \frac{(\rho_s - \rho_l)}{\rho_l} = \frac{(11{,}010 - 10{,}430)}{10{,}430} = 0.0556$$

and the corresponding fractional linear contraction during solidification is calculated as:

$$\frac{\delta L}{L} = \left[1 + \frac{\delta v}{v}\right]^{1/3} - 1 = 1.0556^{1/3} - 1 = 0.0182$$

The bounding lead-to-steel gap, which is assumed filled with air, is calculated by multiplying the nominal annulus radial dimension (4.5 inches in the 125-ton HI-TRAC) by the fractional linear contraction as:

$$\delta = 4.5 \times \frac{\delta L}{L} = 4.5 \times 0.0182 = 0.082 \cdot \text{inches}$$

In this hypothetical lead shrinkage process, the annular lead cylinder will contract towards the inner steel shell, eliminating gaps and tightly compressing the two surfaces together. Near the outer steel cylinder, a steel-to-lead air gap will develop as a result of volume reduction in the liquid to solid phase transformation. The air gap is conservatively postulated to occur between the inner steel shell and the lead, where the heat flux is higher relative to the outer steel shell, and hence the computed temperature gradient is greater. The combined resistance of an annular lead cylinder with an air gap (R_{cyl}) is computed by the following formula:

$$R_{cyl} = \frac{\ln(R_o/R_i)}{2\pi K_{pb}} + \frac{\delta}{2\pi R_i [K_{air} + K_r]}$$

where:

- R_i = inner radius (equal to 35.125 inches)
- R_o = outer radius (equal to 39.625 inches)
- K_{pb} = bounding minimum lead conductivity (equal to 16.9 Btu/ft-hr-°F, from Table 4.2.2)
- δ = lead-to-steel air gap, computed above
- K_{air} = temperature dependent air conductivity (see Table 4.2.2)
- K_r = effective thermal conductivity contribution from radiation heat transfer across air gap

The effective thermal conductivity contribution from radiation heat transfer (K_r) is defined by the

following equation:

$$K_r = 4 \times \sigma \times F_\varepsilon \times T^3 \times \delta$$

where:

$\sigma =$ Stefan-Boltzmann constant

$F_\varepsilon =$ $(1/\varepsilon_{cs} + 1/\varepsilon_{pb} - 1)^{-1}$

$\varepsilon_{cs} =$ carbon steel emissivity (equal to 0.66, HI-STORM FSAR Table 4.2.4)

$\varepsilon_{pb} =$ lead emissivity (equal to 0.63 for oxidized surfaces at 300°F from McAdams, Heat Transmission, 3rd Ed.)

$T =$ absolute temperature

Based on the total annular region resistance (R_{cyl}) computed above, an equivalent annulus conductivity is readily computed. This effective temperature-dependent conductivity results are tabulated below:

Temperature (°F)	Effective Annulus Conductivity (Btu/ft-hr-°F)
200	1.142
450	1.809

The results tabulated above confirm that the assumption of a bounding annular air gap grossly penalizes the heat dissipation characteristics of lead filled regions. Indeed, the effective conductivity computed above is an order of magnitude lower than that of the base lead material. To confirm the heat dissipation adequacy of HI-TRAC casks under the assumed overly pessimistic annular gaps, the HI-TRAC thermal model described earlier is altered to include the effective annulus conductivity computed above for the annular lead region. The peak cladding temperature results are tabulated below:

Annular Gap Assumption	Peak Cladding Temperature (°F)	Cladding Temperature Limit (°F)
None	872	1058
Bounding Maximum	924	1058

From these results, it is readily apparent that the stored fuel shall be maintained within safe temperature limits by a substantial margin of safety (in excess of 100°F).

4.5.1.2 Test Model

A detailed analytical model for thermal design of the HI-TRAC transfer cask was developed using the FLUENT CFD code, the industry standard ANSYS modeling package and conservative adiabatic calculations, as discussed in Subsection 4.5.1.1. Furthermore, the analyses incorporate many conservative assumptions in order to demonstrate compliance to the specified short-term limits with

adequate margins. In view of these considerations, the HI-TRAC transfer cask thermal design complies with the thermal criteria established for short-term handling and onsite transport. Additional experimental verification of the thermal design is therefore not required.

4.5.2 Maximum Temperatures

4.5.2.1 Maximum Temperatures Under Onsite Transport Conditions

An axisymmetric FLUENT thermal model of an MPC inside a HI-TRAC transfer cask was developed to evaluate temperature distributions for onsite transport conditions. A bounding steady-state analysis of the HI-TRAC transfer cask has been performed using the hottest MPC, the highest design-basis decay heat load (Table 2.1.6), and design-basis insolation levels. While the duration of onsite transport may be short enough to preclude the MPC and HI-TRAC from obtaining a steady-state, a steady-state analysis is conservative. Information listing all other thermal analyses pertaining to the HI-TRAC cask and associated subsection of the FSAR summarizing obtained results is provided in Table 4.5.8.

A converged temperature contour plot is provided in Figure 4.5.2. Maximum fuel clad temperatures are listed in Table 4.5.2, which also summarizes maximum calculated temperatures in different parts of the HI-TRAC transfer cask and MPC. As described in Subsection 4.4.2, the FLUENT calculated peak temperature in Table 4.5.2 is actually the peak pellet centerline temperature, which bounds the peak cladding temperature. We conservatively assume that the peak clad temperature is equal to the peak pellet centerline temperature.

The maximum computed temperatures listed in Table 4.5.2 are based on the HI-TRAC cask at Design Basis Maximum heat load, passively rejecting heat by natural convection and radiation to a hot ambient environment at 100°F in still air in a vertical orientation. In this orientation, there is apt to be a less of metal-to-metal contact between the physically distinct entitities, viz., fuel, fuel basket, MPC shell and HI-TRAC cask. For this reason, the gaps resistance between these parts is higher than in a horizontally oriented HI-TRAC. To bound gaps resistance, the various parts are postulated to be in a centered configuration. MPC internal convection at a postulated low cavity pressure of 5 atm is included in the thermal model. The peak cladding temperature computed under these adverse Ultimate Heat Sink (UHS) assumptions is 872°F which is substantially lower than the short-term temperature limit of 1058°F. Consequently, cladding integrity assurance is provided by large safety margins (in excess of 100°F) during onsite transfer of an MPC emplaced in a HI-TRAC cask.

As a defense-in-depth measure, cladding integrity is demonstrated for a theoretical bounding scenario. For this scenario, all means of convective heat dissipation within the canister are neglected in addition to the bounding relative configuration for the fuel, basket, MPC shell and HI-TRAC overpack assumption stated earlier for the vertical orientation. This means that the fuel is centered in the basket cells, the basket is centered in the MPC shell and the MPC shell is centered in the HI-TRAC overpack to maximize gaps thermal resistance. The peak cladding temperature computed for this scenario (1025°F) is below the short-term limit of 1058°F.

As discussed in Sub-section 4.5.1.1.6, MPC fuel unloading operations are performed with the MPC inside the HI-TRAC cask. For this operation, a helium cooldown system is engaged to the MPC via lid access ports and a forced helium cooling of the fuel and MPC is initiated. With the HI-TRAC cask external surfaces dissipating heat to a UHS in a manner in which the ambient air access is not restricted by bounding surfaces or large objects in the immediate vicinity of the cask, the temperatures reported in Table 4.5.2 will remain bounding during fuel unloading operations. Under a scenario in which the cask is emplaced in a area with ambient air access restrictions (for example in a cask pit area), additional means shall be devised to limit the cladding temperature rise arising from such restrictions to less than 100°F. These means are discussed next.

The time duration allowed for the cask to be emplaced in a ambient air restricted area with the helium cooling system non-operational shall be limited to 22 hours. Conservatively postulating that the rate of passive cooling is substantially degraded by 90% (i.e., 10% of decay heat is dissipated to ambient), cladding integrity is demonstrated based on cask heating considerations from the undissipated heat. At a bounding heat load of 28.74kW, the HI-TRAC cask system thermal inertia (19,532 Btu/°F, Table 4.5.5), limits the temperature rise to 4.52°F/hr. Thus, the computed cladding temperature rise during this time period will be less than 100°F.

A forced supply of ambient air near the bottom of the cask pit to aid heat dissipation by the natural convection process is another adequate means to maintain the fuel cladding within safe operating limits. Conservatively assuming this column of moving air as the UHS (i.e. to which all heat dissipation occurs) with no credit for enhanced cooling as a result of forced convection heat transfer, a nominal air supply of 1000 SCFM (4850 lbs/hr) adequately meets the cooling requirement. At this flow rate, the temperature rise of the UHS resulting from cask decay heat input to the airflow will be less than 100°F. The cladding temperature elevation will consequently be bounded by this temperature rise.

4.5.2.2 Maximum MPC Basket Temperature Under Vacuum Conditions

As stated in Subsection 4.5.1.1.4, above, an axisymmetric FLUENT thermal modelof the MPC is developed for the vacuum condition. For the MPC-24E and MPC-32 designs, and for the higher heat load ranges in the MPC-24 and MPC-68 designs, the model also includes an isotropic fuel basket thermal conductivity. Each MPC is analyzed at its respective design maximum heat load. The steady-state peak cladding results, with partial recognition for higher axial heat dissipation where included, are summarized in Table 4.5.9. The peak fuel clad temperatures during short-term vacuum drying operations with design-basis maximum heat loads are calculated to be less than 1058°F for all MPC baskets by a significant margin..

4.5.3 Minimum Temperatures

In Table 2.2.2 and Chapter 12, the minimum ambient temperature condition required to be considered for the HI-TRAC design is specified as 0°F. If, conservatively, a zero decay heat load (with no solar input) is applied to the stored fuel assemblies then every component of the system at steady state would be at this outside minimum temperature. Provided an antifreeze is added to the water jacket (required by Technical Specification for ambient temperatures below 32°F), all HI-TRAC materials will satisfactorily perform their intended functions at this minimum postulated temperature condition. Fuel transfer operations are controlled by Technical Specifications in Chapter 12 to ensure that onsite transport operations are not performed at an ambient temperature less than 0°F.

4.5.4 Maximum Internal Pressure

After fuel loading and vacuum drying, but prior to installing the MPC closure ring, the MPC is initially filled with helium. During handling in the HI-TRAC transfer cask, the gas temperature within the MPC rises to its maximum operating temperature as determined based on the thermal analysis methodology described previously. The gas pressure inside the MPC will also increase with rising temperature. The pressure rise is determined based on the ideal gas law, which states that the absolute pressure of a fixed volume of gas is proportional to its absolute temperature. The net free volumes of the four MPC designs are determined in Section 4.4.

The maximum MPC internal pressure is determined for normal onsite transport conditions, as well as off-normal conditions of a postulated accidental release of fission product gases caused by fuel rod rupture. Based on NUREG-1536 [4.4.10] recommended fission gases release fraction data, net free volume and initial fill gas pressure, the bounding maximum gas pressures with 1% and 10% rod rupture are given in Table 4.5.3. The MPC maximum gas pressures listed in Table 4.5.3 are all below the MPC design internal pressure listed in Table 2.2.1.

4.5.5 Maximum Thermal Stresses

Thermal expansion induced mechanical stresses due to non-uniform temperature distributions are reported in Chapter 3. Tables 4.5.2 and 4.5.4 provide a summary of MPC and HI-TRAC transfer cask component temperatures for structural evaluation.

4.5.6 Evaluation of System Performance for Normal Conditions of Handling and Onsite Transport

The HI-TRAC transfer cask thermal analysis is based on a detailed heat transfer model that conservatively accounts for all modes of heat transfer in various portions of the MPC and HI-TRAC. The thermal model incorporates several conservative features, which are listed below:

i. The most severe levels of environmental factors - bounding ambient temperature (100°F) and constant solar flux - were coincidentally imposed on the thermal design. A bounding solar absorbtivity of 1.0 is applied to all insolation surfaces.

ii. The HI-TRAC cask-to-MPC annular gap is analyzed based on the nominal design dimensions. No credit is considered for the significant reduction in this radial gap that would occur as a result of differential thermal expansion with design basis fuel at hot conditions. The MPC is considered to be concentrically aligned with the cask cavity. This is a worst-case scenario since any eccentricity will improve conductive heat transport in this region.

iii. No credit is considered for cooling of the HI-TRAC baseplate while in contact with a supporting surface. An insulated boundary condition is applied in the thermal model on the bottom baseplate face.

Temperature distribution results (Tables 4.5.2 and 4.5.4, and Figure 4.5.2) obtained from this highly conservative thermal model show that the short-term fuel cladding and cask component temperature limits are met with adequate margins. Expected margins during normal HI-TRAC use will be larger due to the many conservative assumptions incorporated in the analysis. Corresponding MPC internal pressure results (Table 4.5.3) show that the MPC confinement boundary remains well below the short-term condition design pressure. Stresses induced due to imposed temperature gradients are within ASME Code limits (Chapter 3). The maximum local axial neutron shield temperature is lower than design limits. Therefore, it is concluded that the HI-TRAC transfer cask thermal design is adequate to maintain fuel cladding integrity for short-term onsite handling and transfer operations.

The water in the water jacket of the HI-TRAC provides necessary neutron shielding. During normal handling and onsite transfer operations this shielding water is contained within the water jacket, which is designed for an elevated internal pressure. It is recalled that the water jacket is equipped with pressure relief valves set at 60 psig and 65 psig. This set pressure elevates the saturation pressure and temperature inside the water jacket, thereby precluding boiling in the water jacket under normal conditions. Under normal handling and onsite transfer operations, the bulk temperature inside the water jacket reported in Table 4.5.2 is less than the coincident saturation temperature at 60 psig (307°F), so the shielding water remains in its liquid state. The bulk temperature is determined via a conservative analysis, presented earlier, with design-basis maximum decay heat load. One of the assumptions that render the computed temperatures extremely conservative is the stipulation of a 100°F steady-state ambient temperature. In view of the large thermal inertia of the HI-TRAC, an appropriate ambient temperature is the "time-averaged" temperature, formally referred to in this FSAR as the normal temperature.

Note that during hypothetical fire accident conditions (see Section 11.2) these relief valves allow venting of any steam generated by the extreme fire flux, to prevent overpressurizing the water jacket. In this manner, a portion of the fire heat flux input to the HI-TRAC outer surfaces is expended in vaporizing a portion of the water in the water jacket, thereby mitigating the magnitude of the heat input to the MPC during the fire.

During vacuum drying operations, the annular gap between the MPC and the HI-TRAC is filled with water. The saturation temperature of the annulus water bounds the maximum temperatures of all HI-TRAC components, which are located radially outside the water-filled annulus. As previously stated (see Subsection 4.5.1.1.4) the maximum annulus water temperature is only 125°F, so the HI-TRAC water jacket temperature will be less than the 307°F saturation temperature.

Table 4.5.1

EFFECTIVE RADIAL THERMAL CONDUCTIVITY OF THE WATER JACKET

Temperature (°F)	Thermal Conductivity (Btu/ft-hr-°F)
200	1.376
450	1.408
700	1.411

Table 4.5.2

HI-TRAC TRANSFER CASK STEADY-STATE MAXIMUM TEMPERATURES

Component	Temperature [°F]
Fuel Cladding	872
MPC Basket	852
Basket Periphery	600
MPC Outer Shell Surface	455
HI-TRAC Overpack Inner Surface	322
Water Jacket Inner Surface	314
Enclosure Shell Outer Surface	224
Water Jacket Bulk Water	258
Axial Neutron Shield†	258

† Local neutron shield section temperature

Table 4.5.3

SUMMARY OF MPC CONFINEMENT BOUNDARY PRESSURES† FOR NORMAL HANDLING AND ONSITE TRANSPORT

Condition	Pressure (psig)
MPC-24:	
Initial backfill (at 70°F)	31.3
Normal condition	76.0
With 1% rod rupture	76.8
With 10% rod rupture	83.7
MPC-68:	
Initial backfill (at 70°F)	31.3
Normal condition	76.0
With 1% rods rupture	76.5
With 10% rod rupture	80.6
MPC-32:	
Initial backfill (at 70°F)	31.3
Normal condition	76.0
With 1% rods rupture	77.1
With 10% rod rupture	86.7
MPC-24E:	
Initial backfill (at 70°F)	31.3
Normal condition	76.0
With 1% rods rupture	76.8
With 10% rod rupture	83.7

† Includes gas from BPRA rods for PWR MPCs

Table 4.5.4

SUMMARY OF HI-TRAC TRANSFER CASK AND MPC COMPONENTS NORMAL HANDLING AND ONSITE TRANSPORT TEMPERATURES

Location	Temperature (°F)
MPC Basket Top:	
Basket periphery	590
MPC shell	445
O/P† inner shell	280
O/P enclosure shell	196
MPC Basket Bottom:	
Basket periphery	334
MPC shell	302
O/P inner shell	244
O/P enclosure shell	199

† O/P is an abbreviation for HI-TRAC overpack.

Table 4.5.5

SUMMARY OF LOADED 100-TON HI-TRAC TRANSFER CASK
BOUNDING COMPONENT
WEIGHTS AND THERMAL INERTIAS

Component	Weight (lbs)	Heat Capacity (Btu/lb-°F)	Thermal Inertia (Btu/°F)
Water Jacket	7,000	1.0	7,000
Lead	52,000	0.031	1,612
Carbon Steel	40,000	0.1	4,000
Alloy-X MPC (empty)	39,000	0.12	4,680
Fuel	40,000	0.056	2,240
MPC Cavity Water†	6,500	1.0	6,500
			26,032 (Total)

† Conservative lower bound water mass.

Table 4.5.6

MAXIMUM ALLOWABLE TIME DURATION FOR WET TRANSFER OPERATIONS

Initial Temperature (°F)	Time Duration (hr)
115	25.7
120	24.4
125	23.1
130	21.7
135	20.4
140	19.1
145	17.8
150	16.4

Table 4.5.7

INTENTIONALLY DELETED

Table 4.5.8
MATRIX OF HI-TRAC TRANSFER CASK THERMAL EVALUATIONS

Scenario	Description	Ultimate Heat Sink	Analysis Type	Principal Input Parameters	Results in FSAR Subsection
1	Onsite Transport	Ambient	SS(B)	O_T, Q_D, ST, SC	4.5.2.1
2	Lead Gaps	Ambient	SS(B)	O_T, Q_D, ST, SC	4.5.1.1.7
3	Vacuum	HI-TRAC annulus water	SS(B)	Q_D	4.5.2.2
4	Wet Transfer Operation	Cavity water and Cask Internals	AH	Q_D	4.5.1.1.5
5	Fuel Unloading	Helium Circulation	TA	Q_D	4.5.1.1.6
6	Fire Accident	Jacket Water, Cask Internals	TA	Q_D, F	11.2.4
7	Jacket Water Loss Accident	Ambient	SS(B)	O_T, Q_D, ST, SC	11.2.1

Legend:
O_T - Off-Normal Temperature (100°F)
Q_D - Design Basis Maximum Heat Load

ST - Insolation Heating (Top)
SC - Insolation Heating (Curved)
F - Fire Heating (1475°F)

SS(B) - Bounding Steady State
TA - Transient Analysis
AH - Adiabatic Heating

Table 4.5.9

PEAK CLADDING TEMPERATURE IN VACUUM†

MPC	Lower Decay Heat Load Range Temperatures (°F)	Higher Decay Heat Load Range Temperature (°F)
MPC-24	827	960
MPC-68	822	1014
MPC-32	n/a	1040
MPC-24E	n/a	942

† Steady state temperatures at the MPC design maximum heat load reported.

APPENDIX 4.A: CLAD TEMPERATURE LIMITS FOR HIGH-BURNUP FUEL

4.A.1 INTRODUCTION

The current revision of NUREG-1536 [4.A.1] for storage of spent fuel in dry storage casks essentially limits fuel burnup to 45 GWd/MTU. In light of the continuous improvements in fuel bundle design and manufacturing technologies and longer fuel cycles, the quantity of fuel assemblies with burnups in excess of 45 GWd/MTU stored in the spent fuel pools is expected to rise at a rapid pace. It is therefore necessary to address the storage of these high-burnup fuel assemblies in Holtec's storage system. This appendix presents a summary of the methodology developed by Holtec for determining suitable clad temperature limits consistent with the intent of the regulatory review guidelines presented in ISG-15 [4.A.2]. The governing mode for cladding failure, as specified in ISG-15, is assumed to be thermal creep, and the strain limit is set equal to 1% in spite of growing scientific evidence that supports a 2% minimum strain limit. Finally, an alternative criterion for categorizing a spent nuclear fuel (SNF) as "damaged" is proposed in lieu of the ISG-15 criterion which, based on recent data, would needlessly classify a large quantity of high burnup intact SNF as "damaged". This deviation from the guidance contained in ISG-15 has been added to the list of deviations from NUREG-1536 in Table 1.0.3.

4.A.2 REGULATORY GUIDANCE

NRC ISG-15 [4.A.2] presents the current regulatory position on storage and transport of high-burnup spent fuel assemblies. For the purpose of storage in the HI-STORM system, we define high-burnup spent fuel as any fuel assembly with an assembly average burnup greater than 45 GWd/MTU. This definition is consistent with ISG-15.

The mode of failure is postulated to be excessive hoop dilation of the pressurized tubes (fuel rods). Failure is postulated to occur when the cumulative strain reaches 1%. ISG-15 does not prescribe a mathematical model to compute the creep rate: It is incumbent on the certificate holder or licensee to propose an appropriate correlation. In this appendix, we present such a correlation along with the necessary justifications to substantiate its veracity.

ISG-15 also provides a set of fuel integrity criteria predicated on the extent of corrosion (oxidation) of the fuel cladding to define when a high burnup spent nuclear fuel should be treated as damaged. We discuss the ISG integrity criteria vis-à-vis our proposed criteria in a later section in this appendix.

4.A.3 CREEP DEFORMATION MECHANISM AND FAILURE STRAIN

Failure of the fuel cladding in dry storage is postulated to occur from the visco-elastic-plastic effect known as creep. The fuel cladding very gradually dilates in the manner of a pressurized tube under the influence of internal pressure of the contained gas. The predominant stress component in the cladding is the hoop stress, σ, which is readily computed by the classical Lame's formula:

$$\sigma = pr/t \quad (1)$$

where p, r and t are, respectively, the net outward pressure acting on the cladding in dry storage, inside cladding radius and cladding wall thickness.

Classical creep mechanics instructs us that the magnitude of stress, σ, and the coincident metal temperature, T, are the most significant variables in determining the rate of creep for a given material. The development in predicting creep behavior of pure metals and alloys has traditionally followed the path of measuring the creep rate while holding the stress and temperatures constant and then developing a compact mathematical correlation that accords with the measured data. This process, quite logical in light of the absence of an identifiable fundamental constitutive relation for metal creep, has spawned numerous creep equations in the past ninety years. Lin, in his text on creep mechanics [4.A.7] published in 1968, cites eight general correlations: Many more have followed in the years since then. Attempts by the American Society of Metals to correlate the multitude of correlations [4.A.8], each purporting to represent the creep behavior of certain metals and alloys with precision, ended up in an essentially non-specific recommendation that recognizes creep rate as a complex and non-linear function of stress and temperature.

To propose a creep equation for irradiated Zircaloy, an appropriate relationship for strain as a function of stress, temperature and time must be defined. Then the available experimental data on irradiated Zircaloy must be used to correlate and benchmark the functional relationship.

Having developed an experimentally corroborated creep rate functional relationship, the next step in the analysis process is to determine the permissible peak cladding temperature at the start of dry storage that will limit the total creep strain accumulation in the hottest fuel rod in forty years of dry storage to 0.01.

Holtec International has proposed 1% uniform circumferential creep strain of the fuel cladding as a conservative limit for the purpose of establishing the permissible peak cladding temperature, T_p, in dry storage, even though independent work by EPRI [4.A.9], citing several references, including a recent experimental work by Goll [4.A.10], asserts that the 1% strain limit is "overly conservative."

The test creep experiments by Goll et al. [4.A.10] appear to have been expressly performed to establish the failure strain limit of high burnup SNF (54 to 64 GWD/MTU) with a heavy oxide layer (up to ~ 100 μm). To achieve circumferential strains in the range of 2% in a short period, the samples were subjected to a much higher stress (400 to 600 MPa) than would be obtained in dry storage of spent nuclear fuel (<150 MPa). The experiments included 21 creep tests on samples of two rods, none of which failed at 2% hoop strain. Ductility tests on cladding containing radially oriented hydrides also exhibited unbreached integrity at 100 MPa and 423°K, indicating that the increased vulnerability of the fuel cladding in the presence of radially oriented hydride lenses is not a cladding integrity limiting condition.

Oxidation of the cladding during reactor operations is an immutable fact. Oxidation leads to flaking or spalling of the cladding, resulting in a reduction of the tube wall, t, development of a rough external surface (stress raisers) and incursion of hydrogen into the cladding microstructure.

Spalling of the fuel cladding, associated with oxidation of zirconium, is a function of numerous variables, including reactor operation history, water chemistry, areal power density, coolant temperature, and burnup. Spalling or flaking introduces a local surface discontinuity on the cladding surface. However, burst test data on spalled cladding by Garde et al. [4.A.11], if interpreted properly, as shown by EPRI [4.A.9], support the conclusion that a 1% creep strain limit is conservative even for spalled cladding where the hydride lenses, formed as a byproduct of the oxidation process, have penetrated as far as the cladding mid-wall. EPRI [4.A.9] computes the Critical Strain Energy Density (CSED) [4.A.15, 4.A.16] corresponding to the Garde data to be 5 MPa, which corresponds to the fracture toughness value, K_{IC}, of 7.8 MPa$\sqrt{m}$. EPRI computes the K_{IC} for the heavily spalled cladding (up to 50% hydride penetration) at 1% creep to be 3.8 MPa$\sqrt{m}$, thus demonstrating that 1% creep strain limit is conservative. Recent work by Jarheiff, Manzel, and Ortlieb [4.A.17] corroborates EPRI's position by showing that at even up to 2,000 ppm hydride concentration (which will develop only under extremely high levels of burnup), the ductility of irradiated Zircaloy is essentially undiminished.

Failure strain under rapidly applied mechanical loading is a measure of the ductility of the material, which can be significantly lower than the creep strain limit. EPRI [4.A.9] suggests using the strain energy density at failure in burst tests as the invariant parameter to estimate the corresponding creep strain limit for the material. Using this method and typical temperatures and pressures attendant to dry storage, the creep strain limit may be as much as five to ten times the plastic failure strain under burst tests.

Burst tests on irradiated fuel cladding from commercial reactors (Calvert Cliffs Unit 1, ANO Unit 2, Ft. Calhoun) by Garde et al. [4.A.11] show that "ductility of Zircaloy-4 irradiated to fluence levels of 1.2×10^{22} n/cm^2 (E>1 MeV) at LWR operating temperatures of roughly 600°K is about 3 to 4% and depends on the hydride precipitate local volume."

It is generally recognized that the tertiary creep stage [4.A.7, pp. 60-61] is essentially obviated if the material is subject to a constant stress (rather than a constant load, which is common in most engineering applications). Andrade explained the difference between constant load and constant stress creep in 1910: His classical curve [4.A.7, p. 61] is reproduced herein as Figure 4.A.1. The case of irradiated fuel cladding in dry storage, however, belongs to the special class of problems wherein the stress would decrease as the fuel rod containing a fixed quantity of gas at a constant temperature increases in diameter with passage of time due to creep. This is due to the fact that, based on the perfect gas law, the increase in the cladding diameter due to creep reduces the pressure exerted by the contained gas. The increase in diameter also causes a concomitant reduction in the cladding wall thickness. Since the hoop stress σ, governed by Lame's formula (Equation 4.A.1) is proportional to the radius and internal pressure, and inversely proportional to

the wall thickness, it is shown in the following that the hoop stress will remain essentially constant as the cladding radius increases due to creep if the fuel rod were a hollow tube (no fuel pellets) and will decrease if the gas is contained in the annulus between the pellets and the rod.

To quantify the reduction in gas pressure, p, due to creep-induced increase in the rod diameter, let us consider a unit length of a fuel rod of inside radius, r, and initial wall thickness, t, containing a fuel pellet of radius a. The pellet is assumed to be rigid and the gas is assumed to be confined to the annular region defined by radii r and a. If the inner radius of the rod expands to $(r+\Delta r)$ due to creep, then the annular space will accordingly increase, reducing the gas pressure to say, p'. p' is related to p by the perfect gas law:

$$p' \left[(r + \Delta r)^2 - a^2\right] = p\,(r^2 - a^2)$$

Neglecting the terms of second order, we have

$$p' = \frac{p\,b^2}{b^2 + 2\,r\,\Delta r} \tag{2}$$

where we have defined

$$b^2 = r^2 - a^2 \tag{3}$$

Since the increase in circumference of the rod due to increase in radius by Δr causes a corresponding decrease in the rod wall thickness by Δt to maintain a constant metal volume, we have

$$2\pi\,(r + \Delta r)\,(t - \Delta t) = 2\pi r t$$

or

$$r\,\Delta t = t\,\Delta r \tag{4}$$

The initial stress σ is given by Equation (1), the final stress σ' after creep to radius Δr is given by

$$\sigma' = \frac{p'\,(r + \Delta r)}{(t - \Delta t)} \tag{5}$$

Substituting for p' from Equation (2), utilizing Equation (4), and neglecting terms of higher order, we obtain

$$\sigma' = \frac{pr}{t}[1 - \frac{2\Delta r}{r}\frac{a^2}{b^2}] \tag{6}$$

The fractional decrease in stress is given by Eqs. (1) and (6); we have

$$\frac{\sigma - \sigma'}{\sigma} = \frac{\Delta\sigma}{\sigma} = 2\,c\,\chi^2 \tag{7}$$

where

$$c = \frac{\Delta r}{r} \tag{8}$$

and:

$$\chi^2 = \frac{a^2}{r^2 - a^2} \tag{9}$$

We note that in the case of a hollow tube (i.e., no pellets, a = 0), χ=0 and $\Delta\sigma = 0$, i.e., the hoop stress will not change with creep. However, for the case of a fuel rod containing pellets (the real life case), the drop in the stress level with creep is a strong function of χ. If we assume that a = .99r, then χ^2 = 49.25. Using Equation (7), we find that the percentage reduction in stress is 98.5%, corresponding to 1% creep ($c=\Delta r/r$ = .01). In a fuel rod, the gas is in the annulus as well as in the plenum. For a typical fuel rod, EPRI [4.A.9] estimates that the reduction in stress is 17% for 1% creep.

In view of the foregoing, the condition of rapid straining leading to gross rupture that characterizes failure in the tertiary creep domain can be ruled out for fuel cladding in dry storage (Figure 4.A.1). In fact the state of hoop stress in the fuel cladding suffers additional decrease as the heat emission rate from the fuel declines, resulting in the decrease of the gas temperature (and hence, pressure) inside the rods.

To summarize:

- The process of creep will result in a reduction in the cladding hoop stress even if the gas temperature were to remain constant.
- The continuous reduction in the heat emission rate from the fuel correspondingly reduces the gas temperature in the fuel rods, leading to an additional reduction in the hoop stress.
- Creep in fuel rods in dry storage belongs to the special class of problems where the actuating stress decreases with time, thus inoculating the fuel rod against tertiary creep (which is characterized by rapid deformation).

Finally, a fundamental characteristic of creep in metals is its relationship to the mechanical properties of the material. The rate of creep is known to decrease monotonically with the increase in yield strength. The creep strain limit also reduces as the ductility of the material

(measured by its "elongation" in the terminology of ASTM) is reduced. The effect of irradiation is to modify Zircaloy's microstructure resulting in an increase in the yield strength and reduction in the ductility. This would imply a reduced rate of creep and a lower creep limit for the irradiated cladding than its unirradiated counterpart. However, both the yield strength and elongation curves tend to flatten out at high burnup levels (fluence $\approx 10^{22}$ N/cm^2 (E > 1 MeV)) [4.A.12, 4.A.13], suggesting that the Holtec creep equation and 1% creep limit will remain conservative for burnups up to 68,400 MWD/MTU.

4.A.4 ZIRCALOY CREEP STRAIN MODELING: PRIOR WORK

An experimental program to compile creep data on internally pressurized irradiated Zircaloy fuel cladding has been carried out jointly by GNB and Siemens AG [4.A.3]. In this experimental study, internally pressurized Zircaloy samples were irradiated for 10,000 hours at a variety of temperatures and hoop stresses. Test temperatures for each sample were held constant over the entire irradiation period and ranged from 250°C to 400°C. Hoop stresses are temperature dependent and were also, therefore, held constant for each sample over the entire irradiation period and ranged from 80 MPa to 150 MPa. Creep was measured for up to 10,000 hours.

The GNB/Siemens researchers also proposed an empirical model that could be used to predict cladding creep as a function of the cladding hoop stress and temperature. Their model, which we henceforth refer to as the "Siemen's model", is fully described in Reference [4.A.3] and is, therefore, merely summarized in this subsection. The Siemen's creep equation is given as:

$$\varepsilon = At^m \tag{10}$$

where:

ε = the total creep strain at time t (%)
A = the so-called "initial creep strain" (%)
t = the storage time (hr)

The exponent 'm' on the time value in Equation (10) is expressed as a high-order polynomial function as:

$$m = \sum_{i=1}^{11} c_i \times T_f^{i-1} \tag{11}$$

In Equation (11), the c_i values are constants and T_f is a function of hoop stress and the temperature. The constants are given as:

$c_1 = 0.361705\times10^{-13}$
$c_2 = 0.500028\times10^{-3}$
$c_3 = -0.555901\times10^{-6}$
$c_4 = 0.715481\times10^{-7}$
$c_5 = -0.181897\times10^{-8}$
$c_6 = 0.207254\times10^{-10}$
$c_7 = -0.126131\times10^{-12}$
$c_8 = 0.433320\times10^{-15}$
$c_9 = -0.835848\times10^{-18}$
$c_{10} = 0.842689\times10^{-21}$
$c_{11} = -0.345181\times10^{-24}$

and T_f is given as:

$$T_f = T + (\sigma - 80) \times \frac{45}{70} \tag{12}$$

where:

T is the cladding temperature (°C)
σ is the cladding hoop stress (MPa)

Equation (12) is held in the Siemen's formulation to be valid for temperatures between 100°C and 400°C and for hoop stresses between 80 MPa and 150 MPa.

As stated above, we refer to the modeling approach embodied in Equations (10) through (12) as the Siemen's model. This model does, however, have some shortcomings.

Figure 10 of a paper by Dr. Martin Peehs [4.A.4], using the recommended [4.A.3] initial creep strain (A) of 0.04% shows that the Siemen's model more closely approximates the creep behavior of unirradiated Zircaloy and is inordinately conservative for irradiated Zircaloy. As the model is intended for use in determining clad temperature limits for high-burnup fuel assemblies, this might result in erroneous low temperature limits.

The perceived over-conservatism in the Siemen's correlation was empirically remedied in the recent WESFLEX application [4.A.5] by dividing the cumulative creep predicted by the Siemen's model by a factor of two.

Unfortunately, the Siemen's model correlates poorly with the recent creep data published by Goll et al. [4.A.10]. Therefore, it was decided to develop a creep equation for irradiated Zircaloy, using standard procedures, that benchmarks satisfactorily with all publicly available data.

4.A.5 IRRADIATED ZIRCALOY TEST DATA

In this section, we provide a listing of all test data that is utilized herein to benchmark the proposed Holtec creep model. The test data that we are seeking to utilize pertains to experimentally measured creep in irradiated Zircaloy. Although the published data in this area are admittedly sparse, cited bibliographies and public-domain documents have been reviewed to adequately cover the range of stress and temperature conditions in dry storage.

Five sources of creep data are identified for benchmarking the Holtec creep model. The first data source is from the published creep results by Spilker et al. [4.A.3]. The test conditions are:

Temperature:	400°C
Stress:	70 MPa
Time:	1,000-6,000 hrs.

The second data source is from the Kaspar et al. high temperature creep data reported in a docketed dry storage document [4.A.22]. The test conditions for this data are:

Stress:	86 MPa
Temperature:	380°C (0-1,000 hrs)
	395°C (>1,000 hrs)
Time:	1,000-8,000 hrs

The third source of data is from the accelerated creep testing by Goll et al. [4.A.10]. The testing was done on samples of Zircaloy cladding from fuel rods of up to 64,000 MWD/MTU burnup. The test conditions are summarized below:

Stress:	320 MPa to 630 MPa
Temperature:	300°C to 370°C
Time:	2 to 189 hrs

The fourth source of data is from the low temperature creep testing by Einziger and Kohli [4.A.20] on irradiated Turkey Point fuel rods. A total of five pressurized rods were tested at 323°C for a time period of between 31 to 2,101 hrs, and stress of between 146 MPa to 157 MPa. Four of the rods lost their pressure because of an end cap brazing failure.

The test conditions for the rod (TPD04-H6) that retained its pressure are:

Temperature:	323°C
Stress:	146 MPa
Time:	2,101 hrs
Cladding Strain:	0.157%

The fifth data source is from the low temperature creep testing by Kaspar et al. [4.A.21] on irradiated KWO samples. The test conditions are:

Temperature:	350°C
Stress:	50 MPa
Time:	1,000 to 8,000 hrs

4.A.6 PROPOSED CORRELATION (HOLTEC MODEL)

The experimental data cited in the foregoing provides us with creep data for different stress levels up to about 600 MPa and for different temperatures (up to 400°C). While the database is admittedly not copious, it is adequate to provide the means to establish the coefficients in a creep equation of standard form, which, according to classical creep mechanics [4.A.7; 4.A.19, p. 95] should have the following key characteristics:

i. The accumulated creep bears a hyperbolic function relationship to the hoop stress, σ, i.e.,

$$\varepsilon \sim \sinh(\gamma\sigma)$$

ii. The temperature dependence (T) of the accumulated creep follows the Arrhenius equation; $\varepsilon \sim \exp\left(-\frac{\zeta}{RT}\right)$

where ζ is the activation energy, R is the universal gas constant, and T is the absolute temperature.

iii. Recognizing that the test data exhibits continuously decreasing creep rate (i.e., the slope of the creep-time curve is continuously decreasing), the correlation should be appropriate for primary creep of the form $\varepsilon \sim \tau^{\beta}$ where $\beta < 1$, and τ is the time coordinate.

In other words, the Holtec creep model constructed from the above three functional elements is of the form:

$$\varepsilon = \alpha \exp\left(-\frac{\zeta}{RT}\right) \sinh(\gamma\sigma)\, \tau^{\beta} \qquad (13)$$

where α, ζ, γ, and β are creep constants with values suitably selected to bound all relevant irradiated cladding creep data and R is the Universal Gas constant (8.31 J/(g-mol°K)). Differentiating ε with τ will give the rate of creep, φ, as a function of time.

$$\varphi = \frac{d\varepsilon}{d\tau} \qquad (14)$$

The correlation provided in Equation (13) is applicable in the primary creep stage. Creep is assumed to transition into the secondary regime when ε reaches 0.5%.

Figures 4.A.2-4.A.5 show the creep rate predicted by the proposed Holtec creep model against the previously discussed test data. Five principal sources of creep data are identified for benchmarking the creep model. The first data source is shown plotted in Figure 4.A.2 from the Spilker et al. experiments on irradiated fuel rods. The second data source is the Kaspar et al. irradiated cladding creep strain results shown plotted in Figure 4.A.3. The third source of data is by Goll et al. [4.A.10]. The data from the first two sources was essentially at constant stress and temperature and strain was measured at several instants in time. The family of creep strain vs. time relationships are therefore amenable to a graphical representation in a single plot. In contrast, the Goll et al. data is a single creep strain measurement at the end of each experiment at a stress and temperature that was different in each experiment. The stress and temperature range

for the experiments covered a large band (320 to 630 MPa & 300 to 370°C). Therefore, to display the benchmark results from the collected data, a scatter plot of the experimental creep strain vs. Holtec model creep strain is provided in Figure 4.A.4. A straight line representing the ordinate equal to experimental creep strain is shown to aid the reader in confirming that in all cases the Holtec model correlates with the measured creep strain with suitable margins.

For the Einziger and Kohli [4.A.20] creep strain data on the intact TPD04-H6 rod sample, the Holtec Creep Model computes a creep strain of 0.191%. This bounds the measured creep strain of 0.157% by a respectable margin (21.6%). A comparison of the Holtec creep model predictions for the KWO creep testing conditions [Kaspar et al., 4.A.21] is shown in Figure 4.A.5. The Holtec predictions bound the KWO creep curve over the range of time (0 to 8,000 hrs). In the 4,000 to 8,000 hrs time interval, the Holtec model exhibits a diverging trend from the KWO creep curve in the conservative direction. In other words, the slope of the Holtec creep model is steeper than the Kaspar et al. creep curve. Thus, creep strain beyond 8,000 hrs is overestimated by the Holtec creep model.

It is quite obvious from the foregoing that the proposed correlation accords well with the available test data, bounding some with large margins. It is thus established that the proposed creep equation is suitable to bound (not predict) the rate of creep that high burnup fuel in dry storage will sustain with the passage of time.

4.A.7 APPLICATION TO STORAGE IN HI-STORM

Equation (13) provides an appropriate vehicle for computing the accumulated creep over a time, say τ^*, if the stress σ and metal temperature, T, are known. If σ and T are varying with time, then the accumulated creep ε will be calculated by integrating the rate of creep ϕ ($\phi = d\varepsilon/d\tau$) over the time period in dry storage. Therefore, in the HI-STORM system, where σ and T decrease with time, the total creep ε is computed by

$$\varepsilon = \int_0^{\tau} \varphi \, d\tau \qquad (15)$$

where $$\varphi = \frac{d\varepsilon}{d\tau}$$

ε is given by Equation (13). The creep rate, ϕ, like ε, is a function of σ and T.

Hoop stress is directly proportional to internal pressure, which itself is a function of the gas temperature. The fuel temperatures in dry storage casks like the HI-STORM system, however, are not constant but rather decrease over the duration of the dry storage period. To accurately predict the fuel cladding creep strain, this time-varying temperature behavior must be properly incorporated.

It is recognized that the stress σ in a fuel rod will depend on its radius to cladding thickness ratio and internal pressure. Referring to the table of SNF types (Tables 4.3.3 and 4.3.6), it is evident that the r/t ratio varies widely among the various SNF types. To establish a common peak cladding temperature (PCT) limit for all SNF of a given type, we select one upper bound r/t ratio for PWR fuel and one for BWR fuel so that all SNF types included in this FSAR are covered. We assume:

$$w = r/t = 10.5 \text{ (PWR fuel)} \tag{16a}$$

$$w = r/t = 9.5 \text{ (BWR fuel)} \tag{16b}$$

For a specific SNF, defined by cladding thickness t_g and internal radius r, Equations 16a and 16b imply that a certain amount of its wall thickness, Δ, is not recognized in the hoop stress computation. Δ is given by:

$$\text{For PWR fuel; } \Delta = t_g - \frac{r}{10.5} \tag{17a}$$

$$\text{For BWR fuel; } \Delta = t_g - \frac{r}{9.5} \tag{17b}$$

Δ represents the cladding unused thickness not accounted for in the creep analysis and, hence, can be viewed as the "corrosion reserve" in the specific SNF type. Having defined an upper bound r/t, we now need to use an upper bound internal pressure at the start of dry storage to establish the hoop stress, σ, at the beginning of dry storage. In Section 4.3.1, the upper bound of the internal pressure p_r is set at 2,000 psi and 1,000 psi, respectively, for PWR and BWR SNF at the reference temperature θ_r (θ_r = 387°C (PWR), 311°C (BWR)). Both the PWR and BWR cladding internal pressure values, as discussed in Section 4.3.1, are quite conservative.

The stress in the fuel cladding is given by the Lame's formula (Equation (1)).

Using the r/t value given by Equations (16a) and (16b) above, the hoop stress in the cladding at the gas temperature, θ_r, is given as:

$$\begin{aligned} \sigma &= (10.5)\,(2{,}000) = 20{,}500 \text{ psi or } 144.7 \text{ MPa (PWR)} \\ &= (9.5)\,(1{,}000) = 9{,}500 \text{ psi or } 65.5 \text{ MPa (BWR)} \end{aligned} \tag{18}$$

In the next step it is necessary to define the variation of hoop stress σ with time. The internal pressure, p, in the fuel rod (and, therefore, σ through Lame's equation) will decrease with the passage of time due to two discrete effects: (i) creep-induced increase in the cladding diameter explained in Equation (7) and Subsection 4.A.3 above, and (ii) reduction in the bulk temperature of the contained gas due to the monotonic decline in the heat generated by the stored SNF.

For conservatism, the creep-induced pressure reduction is neglected completely. The reduction in the cladding internal pressure due to the continuing reduction in the heat emission rate is determined by ascertaining the rod bulk gas temperature, θ, as a function of time (in storage in HI-STORM).

The internal gas pressure p corresponding to gas temperature θ (in °C) is given by the perfect gas law

$$p = \frac{p_r\,(\theta + 273)}{(\theta_r + 273)} \tag{19}$$

where p_r = 2,000 psi and 1,000 psi for PWR and BWR SNF, respectively.

Using Equation (1), the corresponding stress σ is given by

$$\sigma = \frac{p_r\,(\theta + 273)}{(\theta_r + 273)}\,\frac{r}{t} \tag{20}$$

It is recognized that both the cladding temperature, T, and gas temperature, θ, depend on the system heat generation rate, Q, and the thermal characteristics of the storage system (HI-STORM). Because the HI-STORM system is certified to store a large array of PWR and BWR SNF types, it is necessary that the T and θ functions be defined in a conservative manner to bound all SNF types (a conservative T or θ function means one whose attenuation with time is "less steep" than all SNF types covered by the CoC.) For this purpose, we must first define the heat generation decay function (η) in a conservative manner. Recognizing that the Q(τ) function will attenuate least rapidly with time, τ, for bounding burnup (b) and uranium content in the SNF, we select b=70 GWD/MTU and the B&W 15x15 SNF (uranium content = 495 kg) as the reference PWR SNF. Henceforth, we will refer the SNF with the bounding burnup and uranium content simply as the "bounding SNF". For the same reason, we select GE 7x7 as the reference BWR SNF. The η functions for the reference PWR and BWR SNF are shown in Figure 4.A.6 and 4.A.7, respectively. In Figures 4.A.6 and 4.A.7, η is plotted as the ratio of heat generation of the "bounding SNF" to that at PCDT = 5 years.

In the next step, the HI-STORM 100 thermal model (described in Chapter 4) was used for discrete values of Q to determine T and θ as a function of Q. Strictly speaking, the T and θ functions will be very slightly different for the different MPC types (because of the small differences in their gross heat dissipation capacities). The analytical (curve fit) relationships developed for T(Q) and θ(Q) are accordingly developed to bound the curves obtained by the HI-STORM thermal model analysis. Figure 4.A.8 shows the postulated T(Q) curve and the computed T(Q) curve using FLUENT for MPC-24 to illustrate the conservatism. Likewise, Figure 4.A.9 shows the postulated θ (Q) curve and the computed θ(Q) using FLUENT for hottest

PWR canister (MPC-24). T (Q) and θ(Q) plots for BWR fuel are provided in Figures 4.A.10 and 4.A.11.

These enveloping θ(Q) and T(Q) curves along with the appropriate η(τ) curve (Figure 4.A.6) for PWR SNF and Figure 4.A.7 for BWR SNF) are essential for utilizing the Holtec creep model. The T curve (cladding metal temperature), of course, is the direct input variable in the creep equation. The θ curve, through Equation (20), provides the means to compute the hoop stress, σ, as a function of the time coordinate.

The procedure to compute the peak cladding temperature (PCT) limit using the creep equation (Equation 13) for the HI-STORM system to store an MPC containing SNF of a certain age (post-core decay time (PCDT)) can now be outlined.

Let τ_o denote the PCDT at which the SNF is placed in dry storage in HI-STORM. The object is to calculate the PCT, T_p, such that the accumulated creep in 40 years of storage is 1%.

In other words, the mathematical problem resolves to computing T at $\tau = \tau_o$ such that ε_s is 1%; i.e.,

Determine T at $\tau = \tau_o$ such that

$$\varepsilon_s = \int_{\tau_o}^{\tau_o+\tau^*} \varphi(\sigma, T) \leq 1\% \qquad (21)$$

where τ_o is the PCDT at which the SNF is placed in dry storage, τ^* = the design life of 40 years.

The problem of determining the permissible initial cladding temperature T_p when the fuel is placed in dry storage such that the value of the integral (in Equation 21) is equal to 1% requires an iterative analysis with assumed values of the initial fuel cladding temperature, T_o. The computation proceeds as follows:

i. Assume a value of the peak cladding temperature at τ_o (say T_o).
(τ_o is the post-core decay time at which the SNF is placed into dry storage)

ii. Use the T-Q curve (Figure 4.A.8 or 4.A.10, as applicable) to obtain the associated value of the heat generation rate, Q_o.

iii. From Figure 4.A.9 or 4.A.11 as applicable, obtain the associated value of the gas temperature, θ_o. Equation (20) provides the associated hoop stress, σ_o.

iv. With T_o and σ_o defined, the rate of creep, φ, is provided by Equation (14).

v. To compute the value of φ, at the next time step ($\tau_o + \Delta\tau$), updated values of σ and T are required. For this purpose, the coincident heat generation rate Q is obtained by using Figure 4.A.6 or 4.A.7, as applicable, which provides Q at any time τ through the simple algebraic relationship

$$Q = \frac{Q_o \eta}{\eta_o} \tag{22}$$

where η is the value of the dimensionless heat generation rate at the PCDT of interest, and η_o is the corresponding value at τ_o (PCDT at the initiation of dry storage). Figure 4.A.8 (or 4.A.10) and 4.A.9 (or 4.A.11), respectively, provide the associated T and θ. Equation (20) provides the associated σ. This process is repeated at incremental time steps. In this manner, time history of σ and T as a function of τ (starting at σ_o and T_o computed for $\tau = \tau_o$) is obtained for the 40-year duration.

vi. Equation (21) is used to compute the total accumulated creep, ε_s, in 40 years (τ^* = 40 years).

vii. If the value of ε_S is greater than 1%, then the initial assumed value of the peak cladding temperature, T_o, is appropriately adjusted and the calculation returns to Step (i) above.

viii. The process is repeated until the computed ε_s is close to 1% within a small tolerance (set equal to 0.001) in the numerical analysis. The converged value of T_o is the permissible cladding temperature (T_p) for fuel placed in dry storage at PCDT = τ_o.

4.A.8 ALLOWABLE CLAD TEMPERATURE LIMITS

Using the Holtec creep model described in the preceding section, allowable peak clad temperature limits for high-burnup fuel assemblies have been determined. These calculated temperature limits are presented in Table 4.A.1, below.

Table 4.A.1
Allowable Peak Clad Temperature Limits for High Burnup Fuel from Holtec Creep Model

Fuel Age at Initial Loading	PWR Fuel Limit	BWR Fuel Limit
5 years	361.55°C [682.79°F]	397.63°C [747.73°F]
6 years	358.00°C [676.40°F]	393.49°C [740.28°F]
7 years	354.80°C [670.64°F]	390.26°C [734.47°F]
10 years	349.15°C [660.47°F]	384.49°C [724.08°F]
15 years	345.78°C [654.40°F]	380.95°C [717.71°F]

The temperature limits in Table 4.A.1, it should be recalled, are obtained using a most conservative equation of state for creep, a bounding value of internal gas pressure at the start of fuel storage, an upper bound value for cladding radius-to-thickness ratio (10.5 for PWR and 9.5 for BWR fuel), and a 1% limit on creep deformation in 40 years of storage. To build in even additional margins in the allowable heat load for the MPCs, the PCT limit is further reduced, as shown in Table 4.A.2. The values in Table 4.A.2 are the ones used in the thermal analysis in Chapter 4. The PCT limits in Table 4.A.2, as can be ascertained by direct comparison with Table 4.A.1, are as much as 39.85°C less. This additional margin in the PCT limits, admittedly not typical in dry storage applications, has been provided as a first step is addressing the issue of dry storage of high burnup fuel, and may be re-visited.

Table 4.A.2
High Burnup Fuel Allowable Peak Clad Temperature Limits Used in the Thermal Analysis in Chapter 4

Fuel Age at Initial Loading	PWR Fuel Limit	BWR Fuel Limit
5 years	359.7°C [679°F]	393.2°C [740°F]
6 years	348.7°C [660°F]	377.9°C [712°F]
7 years	335.0°C [635°F]	353.7°C [669°F]
10 years	327.2°C [621°F]	347.9°C [658°F]
15 years	321.9°C [611°F]	341.1°C [646°F]

4.A.9 INTACT AND DAMAGED FUEL

ISG-15 states that for a fuel assembly to be considered intact, the following criteria must be met:

> "A1. No more than 1% of the rods in the assembly have peak cladding oxide thicknesses greater than 80 micrometers.
>
> A2. No more than 3% of the rods in the assembly have peak cladding oxide thicknesses greater than 70 micrometers."

ISG-15 provides the bases for the conditions and guidelines presented above. The limits on cladding oxide thickness are intended to ensure that the hydrogen concentration in the cladding micro-structure does not exceed 400 to 500 parts per million. The creep strain limit of 1%, along with hydrogen concentration limits, are intended to ensure that cladding perforation does not occur. Specifically, ISG-15 states:

> "The staff believes that Zircaloy cladding can withstand uniform creep strains (i.e., creep prior to tertiary or accelerating creep strain rates) of about 1% before the cladding can become perforated if the average hydrogen concentration in the cladding is less than about 400 to 500 parts per million (ppm). This amount of hydrogen corresponds to an oxide thickness of approximately 70-80 micrometers using the recommended hydrogen pickup fraction of 0.15 from Lanning, et al, and Garde. The staff also believes that the strength and ductility of irradiated Zircaloy do not appear to be significantly affected by corrosion-induced hydrides at hydrogen concentrations up to approximately 400 ppm.

According to ISG-15, the thickness of the cladding oxide layer needs to be determined prior to loading for high burnup fuel. Only those high-burnup fuel assemblies that meet both of the oxidation conditions presented above may be stored as intact; all other assemblies must be treated as potentially damaged fuel. This, as we discuss below, is an overly restrictive requirement, which has prompted Holtec to propose an alternative criterion for damaged fuel as an approved deviation from this regulatory guidance.

Available cladding thickness measurement data on high burnup SNF is quite sparse. However, recent data collected by a Westinghouse .PWR owner indicates that the oxidation-induced cladding metal loss can be well in excess of 80μm in a substantial fraction of the population of high burnup fuel. All fuel rods that had experienced a heavy oxide corrosion, however, were found to be intact, i.e., none exhibited loss of pressure boundary integrity. Corrosion data compiled in Japan [4.A.23] reproduced in Figures 4.A.12 and 13 show that the corrosion loss increases rapidly with increasing burnup. In view of the data in Figures 4.A.12 and 13, applying the ISG-15 criteria will a' priori consign hundreds of undamaged, high burnup fuel assemblies already stored in the plant's fuel pool to the potentially damaged category. This experience is sure to be repeated at other plants when measurements are taken. Clearly, the oxidation threshold for defining damaged SNF warrants additional consideration.

To propose a technically sound cladding corrosion limit, we must consider two underlying facts, namely: (i) the collateral effect of cladding oxidation on its creep capacity and (ii) the increase in circumferential stress due to loss in the cladding wall thickness.

The effect of cladding oxidation on the creep limit of the cladding material has been assayed by EPRI [4.A.18]. EPRI recommends a 2% creep strain limit for high burnup fuel that may have sustained spallation in the reactor core. Our proposed strain limit of 1% quite clearly provides a significant additional margin over the EPRI/NEI recommendation.

If the 1% creep strain limit is accepted for the spalled cladding, then it is possible to define the acceptable metal loss (oxidation loss) using the hoop stress as the guiding parameter. It is recalled that the computation of the creep strain in Section 4.A.8 in the foregoing has been performed for σ_o = 144.7 MPa for PWR SNF and 65.5 MPa for BWR SNF, where σ_o = the hoop stress in the fuel cladding at the beginning of dry storage. Furthermore, the internal gas pressure in the cladding, at the beginning of dry storage, p_o, has been assumed to be equal to 2000 psi and 1000 psi for PWR and BWR SNF, respectively. Using Lame's formula, the maximum cladding stress (σ_o) is computed as the product of p and cladding radius to thickness ratio, w. The value of w has been set as 10.5 and 9.5 for PWR and BWR fuel, respectively, in the calculation of accumulated creep (Section 4.A.8).

In other words, the initial stress σ_o used in the creep analysis in this appendix uses the limiting values of p and r/t as shown in Table 4.A.3.

Table 4.A.3

Assumed Pressure Geometry Parameters for Creep Analysis

	Internal Pressure at the Start of Storage	**w = r/t**	**Stress σ_o Computed by Lame Formula**
PWR Fuel	2,000 psi	10.5	144.7 MPa
BWR Fuel	1,000 psi	9.5	65.5 MPa

PWR and BWR fuel assemblies used in commercial reactors in the U.S. have lower values of w than the number used in the creep analysis herein (Table 4.A.3). The metal wall in the as-fabricated fuel in excess of that implied by the value of w in the above table therefore is the available corrosion allowance, Δ. Tables 4.A.4 and 4.A.5 provide the values of Δ using Equation (17) for different PWR and BWR fuel classes using the thinnest cladding assembly type within each class (fuel assembly types in any one class have the same rod O.D. and pitch, but may have different cladding thicknesses). It is evident from these tables that the available Δ in all fuel assembly array/classes is well in excess of 100μm.

In view of the information presented in the foregoing, it is proposed that the permitted maximum cladding corrosion be specified so that the value of w in Table 4.A.3 for high burnup fuel is preserved.

Table 4.A.4

Available Corrosion Reserve in PWR Fuel Cladding

Holtec Fuel Assembly Array/Class*	Nominal Cladding Outer Diameter (in.)	Nominal Cladding Thickness (in.)	Available Corrosion* Reserve (μm),Δ
14×14A	0.4	0.0243	192
14×14B	0.417	0.0243	165
14×14C	0.44	0.026	191
15×15A	0.418	0.026	217
15×15B	0.42	0.024	159
15×15C	0.417	0.03	321
15×15D	0.43	0.025	175
15×15E	0.428	0.0245	163
15×15F	0.428	0.023	122
15×15H	0.414	0.022	111
16×16A	0.382	0.025	233
17×17A	0.36	0.0225	190
17×17B	0.372	0.0205	120
17×17C	0.377	0.022	156

* Fuel Assembly Array Classes are defined in Section 6.2

* Any form of corrosion that produces non-adherent (flaked or spalled) metal layers should be considered to be lost for load (pressure) bearing purposes.

Table 4.A.5

Available Corrosion Reserve in BWR Fuel Cladding

Holtec Fuel Assembly Array/Class*	Nominal Cladding Outer Diameter (in.)	Nominal Cladding Thickness (in.)	Available Corrosion* Reserve (μm),Δ
7×7B	0.563	0.032	145
8×8B	0.484	0.034	295
8×8C	0.483	0.032	252
8×8D	0.483	0.03	196
8×8E	0.493	0.034	295
9×9A	0.440	0.028	197
9×9B	0.433	0.026	151
9×9C	0.423	0.0295	262
9×9D	0.424	0.03	275
9×9E	0.417	0.0265	186
9×9F	0.417	0.0265	186
9×9G	0.424	0.03	275
10×10A	0.404	0.026	189
10×10B	0.3957	0.0239	141
10×10C	0.378	0.0243	176

* Fuel Assembly Array Classes are defined in Section 6.2

4.A.10 CLOSURE

A mathematical relationship to conservatively estimate the extent of primary creep in the irradiated Zircaloy cladding has been proposed. The form of proposed creep equation is consistent with the classical metal creep formulation wherein the two principal variables, stress and temperature, respectively, bear an exponential and Arrhenius-type relationship to creep accumulation. The creep equation has been validated against available irradiated cladding creep data and shown to correlated with the measured data in the temperature range (300 to 400°C) and stress range (70 MPa – 630 MPa) with considerable margins. This benchmarked creep equation is used to compute the PCT limits for SNF placed in dry storage after a given amount of time in

* Any form of corrosion that produces non-adherent (flaked or spalled) metal layers should be considered to be lost for load (pressure) bearing purposes.

wet storage (wet storage time is also referred to as "fuel age"). In computing the PCT limits, several assumptions have been made to render a conservative prediction. The key conservatisms (in addition to the use of a creep equation that overpredicts creep for a given stress and temperature) are:

i. The maximum permissible creep is set at 1%.

ii. The internal pressure (hence the hoop stress) in the cladding is assumed to remain unchanged due to the creep induced dilation of the rod radius (Equation 7 in Subsection 4.A.3).

iii. The primary creep that is characterized by a monotonically decreasing creep rate with time is assumed to cease when 0.5% creep has been accumulated and the transition to secondary creep is assumed to begin. Thereafter, the creep rate is conservatively held constant for constant stress and temperature.

iv. The bounding burnup of 70 GWD/MTU is used to construct the relationship for decay of heat generation from the stored spent nuclear fuel (Figure 4.A.6 and 4.A.7).

v. The assumed internal rod pressure, which directly affects the level of hoop stress, has been set at a bounding high value for both PWR and BWR SNF.

4.A.11 NOMENCLATURE

K_{IC}: Fracture Toughness

p: Internal gas pressure in the fuel rod

Q: The total heat generation in the HI-STORM 100 MPC.

r: Inside radius of the fuel rod

T: Peak cladding temperature

t: Cladding wall thickness recognized in the hoop stress calculation

t_g: Nominal thickness of the fuel cladding

w: Ratio of r to t

ε: Accumulated creep in dry storage (%)

ε_s: Total accumulated creep in 40 years of storage (%)

τ: Post Core Decay Time (PCTD), i.e., the time elapsed after reactor shutdown

τ_o: PCDT at the time the SNF is placed in dry storage (also known as "fuel age")

θ: Bulk gas temperature in the fuel rod, °C

φ: Rate of creep

σ: Hoop stress in the fuel cladding

η: Ratio of $Q(\tau)$ to Q_o

Subscripts

o: Value of the variable at $\tau = \tau_o$
a: Ambient
r: Reference point

4.A.12 REFERENCES

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