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Recommended Temperature Limits for Dry Storage of Spent Light Water Reactor Zircaloy-Clad Fuel Rods in Inert Gas

May 1987

Prepared for the U.S. Department of Energy
under Contract DE-AC06-76RLO 1830

Pacific Northwest Laboratory
Operated for the U.S. Department of Energy
by Battelle Memorial Institute

PNL-6189

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PACIFIC NORTHWEST LABORATORY
operated by
BATTELLE MEMORIAL INSTITUTE
for the
UNITED STATES DEPARTMENT OF ENERGY
under Contract DE-AC06-76RLO 1830

Printed in the United States of America
Available from
National Technical Information Service
United States Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22161

NTIS Price Codes
Microfiche A01

Printed Copy

Pages	Price Codes
001-025	A02
026-050	A03
051-075	A04
076-100	A05
101-125	A06
126-150	A07
151-175	A08
176-200	A09
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PNL--6189

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RECOMMENDED TEMPERATURE LIMITS FOR DRY
STORAGE OF SPENT LIGHT WATER REACTOR
ZIRCALOY-CLAD FUEL RODS IN INERT GAS

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May 1987

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EXECUTIVE SUMMARY

In 1983, the Commercial Spent Fuel Management Program (CSFM) at Pacific Northwest Laboratory (PNL) recommended a maximum initial cladding temperature limit of 380°C for dry storage of Zircaloy-clad light-water-reactor (LWR) spent fuel in inert gas. In 1985, two U.S. Nuclear Regulatory Commission (NRC) Safety Evaluation Reports (SERs) did not unconditionally accept the recommended 380°C temperature guideline. Instead, the NRC has developed an independent model to be used on a case-by-case basis to calculate allowable temperature limits for inert gas cask storage. Thus, no NRC-accepted codes are available to cask vendors and utilities by which they can calculate fuel rod storage temperature limits.

This study was conducted at PNL to 1) evaluate the need for multiple temperature limits for 40-yr dry storage of LWR fuel rods, 2) recommend maximum temperature limits for all Zircaloy-clad LWR spent fuel to prevent failure by creep rupture during dry storage in inert gas, and 3) provide a procedure by which utilities can easily calculate maximum allowable temperature limits for dry storage of their specific fuel.

To accommodate the effects of variations in fuel design, burnup level, fuel age, and the geometry and makeup of dry storage casks, the CSFM model for predicting temperature limits for dry storage produces a family of generic limit curves. Variations in fuel design and burnup levels result in a range of fuel rod internal pressures. A limit curve is established to describe, as a function of cladding hoop stress, the recommended maximum allowable initial cladding temperature to achieve 40-yr-dry storage. As spent fuel ages, its cooling rate decreases, requiring a lowering of temperature limits. Separate limit curves are established for 5-, 6-, 7-, 10-, and 15-yr fuel ages. Dry storage casks impose their own heat transfer characteristics on the natural cooling rate of the fuel. The temperature decay history model used in generating the limit curves is conservative for a variety of dry storage cask designs.

Independent verification/validation studies were performed on the CSFM model. These studies included sensitivity studies on parameters used in the

model, comparisons of the model's predictions with experience in the Federal Republic of Germany, and comparisons of the model to the NRC DCCG model. The studies verify that the CSFM model is technically sound and defensible. The CSFM model's high level of prediction conservatism compensates for the lack of pertinent data on the ductility of irradiated Zircaloy at the very low creep rates representative of dry storage conditions.

Simple procedures are given in Section 4.0 by which the family of CSFM limit curves can be used to generate fuel-rod-specific limits for both peak-power rods and average-power rods. Calculations based on peak-power rods are recommended because of the availability of the necessary input data from fuel vendors' safety analysis reports. However, rod-specific limits can be calculated for average-power rods (or other specific rods) as well if similar data are available. For those cask designs for which the CSFM model predictions are conservative, no additional procedures are needed to calculate CSFM rod-specific limits. Where utilities or cask vendors wish to use other casks, the necessary parameters could be input to the CSFM temperature decay history model subroutine.

The following can be concluded:

- The recommendation of a single-valued temperature limit of 380°C should be replaced by multiple limits to account for variations in fuel design, burnup level, spent fuel age, and storage cask design. A single-valued limit to account for these factors would, in some situations, impose unnecessary conservatisms and, potentially, economic penalties for utilities and storage cask vendors.
- The technical validity and conservatism of the CSFM model should assure acceptance by the NRC for utility and cask vendor use.

ACKNOWLEDGMENTS

The authors acknowledge the sponsorship of this activity by Virginia Power Co. (VP), Carolina Power and Light (CP and L), the Electric Power Research Institute (EPRI) and the U.S. Department of Energy (DOE). The programmatic support of the Commercial Spent Fuel Management Program (CSFM) at Pacific Northwest Laboratory is also acknowledged.

Specific appreciation is extended to Marvin Smith (VP), David Koss (CP and L), Ray Lambert (EPRI), Jack Collins (DOE), and Gordon Beeman (CSFM).

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1.0 INTRODUCTION

1.1 BACKGROUND

In Technical Basis for Storage of Zircaloy-Clad Spent Fuel in Inert Gases (Johnson and Gilbert 1983), a maximum initial cladding temperature limit of 380°C was recommended for dry storage. This guideline was chosen to represent a relatively conservative position with respect to the then-current experience with Zircaloy-clad light-water-reactor (LWR) fuel rods and to the results of modeling the dry storage behavior of Zircaloy-clad fuel rods. The experience data encompassed a range of temperatures, both above and below the recommended value. The various models also suggested a range of temperature limits that were above and below the recommended value.

On May 30, 1985, the U.S. Nuclear Regulatory Commission (NRC) issued a safety evaluation report (SER) on the Gesellschaft für Nuklear-Service, m.b.H. (GNS) CASTOR-1C Topical Safety Analysis Report (TSAR). In this SER, the proposed 400°C cladding temperature limit for storage of boiling-water-reactor (BWR) fuel in the CASTOR-1C was accepted based on calculations using a stress level of 60.7 MPa as input to a diffusion-controlled cavity growth (DCCG) damage model.

On September 30, 1985, the NRC issued an SER on the GNS CASTOR-V/21 TSAR. In this SER, the proposed 370°C cladding temperature limit at 66 MPa hoop stress was accepted for storage of pressurized-water-reactor (PWR) fuel rods in the CASTOR-V/21 cask, again based upon calculations using the DCCG model.

As a result, several issues were raised:

1. The two SERs did not unconditionally accept the 380°C temperature guideline recommended by Johnson and Gilbert (1983).
2. The NRC had developed an independent model to calculate allowable temperature limits for inert gas cask storage. The model is being used on a case-by-case basis for each cask vendor application; presumably it would also be applied to any means of dry storage that a utility might choose to implement.

3. There are no NRC-accepted codes available to cask vendors and utilities by which they can calculate fuel rod storage temperature limits.

To clarify the methodology for determining temperature limits for dry storage, Virginia Power Company and Carolina Power and Light Company, through their cooperative agreements with the U.S. Department of Energy (DOE), requested Pacific Northwest Laboratory (PNL)^(a) to evaluate and report on these issues.

This report, cooperatively funded by Virginia Power Company, Carolina Power and Light Company, the Electric Power Research Institute, and the U.S. Department of Energy (DOE) through the Commercial Spent Fuel Management (CSFM) Program at PNL, documents the results of the study made to address these issues.

1.2 OBJECTIVES OF THE STUDY

The following objectives were the bases for this study:

- evaluate the need for multiple temperature limits for 40-yr dry storage of LWR fuel rods;
- recommend maximum temperature limits for dry storage that would cover all Zircaloy-clad LWR spent fuel;
- verify that the methodology used to develop these limits is technically sound and defensible; and
- provide a procedure by which utilities can easily calculate maximum allowable temperature limits for dry storage of their specific fuel.

1.3 REPORT ORGANIZATION

In Section 2.0, the need for multiple temperature limits for dry storage of U.S. commercial LWR spent fuel is discussed. This need arises from the

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variety of fuel rod designs, current and projected burnup levels, spent fuel ages (decay periods), and storage cask designs that exist in the national inventory.

The CSFM model for predicting temperature limits for dry storage is described in Section 3.0. The family of recommended generic temperature limit curves generated by the model, applicable to all Zircaloy-clad LWR spent fuel, is also presented. The results of validation analyses that were performed to verify that the CSFM model is technically sound and defensible are presented. Full details of the verification/validation analyses are provided in Appendices A through D.

In Section 4.0 procedures are provided by which a utility or a cask vendor may convert the CSFM generic limit curves into fuel-rod-specific limits for dry storage.

The conclusions from this study are presented in Section 5.0.

2.0 THE NEED FOR MULTIPLE TEMPERATURE LIMITS

Commercial LWR spent fuel in the United States varies with fuel designs, burnup level, and age.^(a) The geometry and makeup of the dry storage casks can be expected to affect the thermal history of the fuel during storage. The effects of these variables on temperature limits for spent fuel dry storage are evaluated.

2.1 VARIATIONS IN FUEL ROD INTERNAL PRESSURES, FUEL DESIGNS, AND BURNUPS

2.1.1 U.S. LWR Fuel Designs and Populations

The U.S. commercial light-water-reactor (LWR) industry utilizes two basic reactor types: the BWR and the PWR. Each of these reactor designs employs fuel assemblies that make up the reactor core and sustain the nuclear chain reaction. Each fuel assembly consists of a square array of fuel rods.

The BWR utilizes two basic fuel assembly designs, a 7 x 7 design and an 8 x 8 design. These designs consist of 49 and 64 rods, respectively, arranged in a square array. Besides the increase in the number of rods, the primary differences between the 7 x 7 and 8 x 8 fuel assembly designs are differences in the fuel rod diameter and operational linear heat generation rate (LHGR) of the fuel rods. The lower LHGR in the 8 x 8 design results in lower fuel temperatures and improved fuel performance for temperature-dependent parameters such as fission gas release. Because of the better fuel performance of the 8 x 8 design, the use of the 7 x 7 design has nearly ceased in BWR plants in the United States. The number of BWR fuel rods of each design irradiated in the United States as of December 1985 is listed in Table 2.1. These figures include the number of rods discharged and the number currently being irradiated.

The PWR plants in the United States utilize four fuel assembly designs, with variations of these basic designs from four fuel vendors. The four basic

(a) The term "age" is used to describe the period from reactor shutdown until initial storage, with the attendant decay in radioisotope inventory and heat.

TABLE 2.1. Total Number of U.S. Commercial LWR Fuel Rods
Irradiated as of December 1985 According to
Type of Assembly (Bailey and Wu 1986)

BWR, millions of rods

7 x 7 Design: 0.7

8 x 8 Design: 2.8

PWR, millions of rods

14 x 14 Design: 1.4

15 x 15 Design: 2.6

16 x 16 Design: 0.3

17 x 17 Design: 2.4

designs are the 14 x 14, 15 x 15, 16 x 16, and 17 x 17 square array designs that consist of 196, 225, 256, and 289 fuel rod positions, respectively, per assembly. As with the BWR assemblies, a larger number of rods per assembly is associated with a smaller fuel rod diameter and a lower operational LHGR per rod. Consequently, the 17 x 17 fuel rods operate at the lowest fuel temperatures of the four PWR designs. The number of fuel rods irradiated as of December 1985 for each PWR design is also listed in Table 2.1.

General Electric Company fuel designs were used for nearly all of the BWR fuel rods irradiated in the United States, with the exception of approximately 200,000 rods of the 8 x 8 design fabricated by the Exxon Nuclear Company. General Electric Company has several variations of the 8 x 8 design, but the 8 x 8R design (a revised 8 x 8 fuel design) represents the largest number of rods irradiated as of 1985 (Bailey and Wu 1986).

Westinghouse Electric Company fuel designs make up the majority (4.2 million) of the PWR fuel rods irradiated in the United States. Westinghouse manufactures its own version of each of the four PWR designs. Most of the 16 x 16 fuel assemblies that have been irradiated in the United States were produced by Combustion Engineering, Inc. Nearly equal numbers of the Westinghouse and Combustion Engineering 14 x 14 fuel assemblies have been irradiated. The majority of the 15 x 15 and 17 x 17 irradiated fuel assemblies are of Westinghouse design.

2.1.2 Effect of Fuel Design on Fuel Rod Internal Pressures

2.1.2.1 General Effects of Fuel Design on Rod Internal Pressures

The major contribution to the stress level (operational or storage) in intact spent fuel rod cladding is internal rod pressure following in-reactor irradiation. The internal pressure is due to 1) the fill gas, usually helium, that is introduced during fabrication of the rod and 2) the release of fission gas during irradiation. The level of initial helium pressure introduced during fabrication by each fuel vendor is proprietary. This pressure is generally less than 10 atmospheres at standard temperature and pressure (STP) for BWR rods and greater than 20 atmospheres, but less than 40 atmospheres (STP), for PWR rods. Because the initial fill-gas pressure for PWR rods is several times higher (as well as because of variations in cladding thicknesses), large differences in cladding stress exist between BWR and PWR spent fuel rods during dry storage. The large difference in fill-gas pressures between BWR and PWR rods is due to two interrelated factors: 1) the primary system pressures differ by a factor of two (1050 psi for BWR versus 2250 psi for PWR), and 2) in-reactor maximum internal rod pressures are not allowed to exceed the system pressure by a significant amount. The amount that some vendors can exceed system pressure is proprietary.

The differences in internal pressures within PWR (or BWR) fuel designs, beyond those resulting from the introduction of fill gas during fabrication, are due to differences in fission-gas release during operation and the fabricated void volumes in the rod. The differences in fission-gas release are primarily the result of differences in operating LHGR and resultant fuel temperatures for the individual designs within a given reactor type (PWR or BWR). As noted earlier, in general, the larger the diameter of the rod (fewer rods per assembly) the higher the rod LHGR. For example, the 7 x 7 BWR fuel rod operates at higher LHGRs and, thus, fuel temperatures are higher than they are in the 8 x 8 fuel rod. Similarly, the 15 x 15 PWR fuel rod operates at higher temperatures than the 17 x 17 fuel rod.

2.1.2.2 Calculated Effects of Design on Fuel Rod Internal Pressure

In order to illustrate the range of internal rod pressures that may exist in both BWR and PWR designs, the 7 x 7, 8 x 8R, 15 x 15, and 17 x 17 fuel designs were selected for evaluation. The results of this evaluation are given in Table 2.2. The 15 x 15 design was selected over the 14 x 14 design because it represents nearly twice as many irradiated rods. The Westinghouse Electric Company 15 x 15 and 17 x 17 designs were selected because they represent the largest number of PWR rods that have been irradiated. The General Electric Company 7 x 7 and 8 x 8R designs were selected because they represent the largest number of BWR rods irradiated.

The GAPCON-THERMAL-2, Revision 2 (GT2R2) fuel performance code (Cunningham and Beyer 1984) was used to determine the range of internal pressures for these designs; these are listed in Table 2.2. The "best estimate" version of this

TABLE 2.2. Range of PWR and BWR Operating Rod Pressures as Calculated for Different Fuel Designs, and Burnup Levels with the GT2R2 Code^(a,b)

Type of Operating Conditions	Fuel Design	Rod-Average Burnup, MWd/kgM	Rod Pressure, psi
Upper Bound	PWR 15 x 15	33	1707
Operating Conditions	BWR 7 x 7	45	2416
		28	1094
Average Operating Conditions	PWR 17 x 17	33	1293
	BWR 8 x 8R	28	142

- (a) Includes influence of void volume (gap, plenum, and dish volume) based on in-reactor operating temperature (Acey and Voglewede 1980; Bailey and Wu 1986).
- (b) Includes 0.004 in. reduction in cladding O.D. to account for waterside cladding corrosion (Gargarelli et al. 1982; Pyecha et al. 1985; and Baumgartner et al. 1982).

code was utilized. This version of the code has been shown (Cunningham and Beyer 1984) to have a small conservative bias in its predictions of fission-gas release and internal rod pressures. GT2R2 is used by the U.S. NRC as an independent audit code. Calculations of both upper-bound and average-rod pressures are presented below.

2.1.2.2.1 Upper-Bound Rod Pressures. The 7 x 7 design for BWRs and the 15 x 15 design for PWRs were selected to provide an upper bound to the rod pressures for each of these reactor types, as shown in Table 2.2. In order to provide an upper-bound estimate of rod pressures, the operating input to GT2R2 for these two fuel rod designs is based on upper-bound values of LHGR. Consequently, the steady-state power histories were based on those that might be expected from the peak rods in a core of these designs.

In addition, the operating input to the code includes ramping these rods in power to the maximum LHGRs that were allowed by the Technical Specifications for these designs (Bailey and Wu 1986; Acey and Voglewede 1980). The ramps to maximize LHGR were performed every 8 MWd/kgM of burnup up to 24 MWd/kgM. Then, the LHGR was decreased 10% every 10 MWd/kgM beyond 24 MWd/kgM to account for fissile material depletion at high burnups. These ramps to the maximum LHGR limit were the primary driving force for the fission-gas release in these calculations.

The actual LHGR in the peak rod in a core is not permitted to exceed the maximum LHGR limit during normal operational transients, and the likelihood of rod powers obtaining the maximum LHGR limit is very small during normal operational modes. Only a small percentage of rods in a commercial reactor core operate at these upper-bound values of LHGR; the probability of a fuel rod in a reactor core actually achieving the Technical Specification LHGR is less than 1%. Consequently, these power histories used for the GT2R2 inputs of the 7 x 7 BWR and 15 x 15 PWR fuel-rod designs represent upper-bound limits of operation; and thus, the GT2R2 calculated rod pressures for these designs represent conservative upper-bound limits. The conservative nature of these calculations is further enhanced by the fact that the GT2R2 version used for this calculation has a small conservative bias of overpredicting fission-gas release. Current

NRC licensing analysis for determining internal fuel rod pressures at operating conditions uses similar methods to assure that design criteria on rod pressures are not exceeded.

It should also be noted that the upper-bound internal rod pressures for the 8 x 8R and 17 x 17 fuel rod designs will be lower than those from the 7 x 7 and 15 x 15 fuel designs at current discharge burnup levels.

2.1.2.2.2 Average Rod Pressures. The 8 x 8R BWR and 17 x 17 PWR fuel designs were selected to illustrate the rod pressures that may be expected from the average rod in an operating core. These two designs are currently the most widely used fuel designs for U.S. BWR plants and PWR plants, respectively. In order to calculate average rod pressures, the power history input to GT2R2 was based on the core-average LHGR for the respective design. This average LHGR was maintained throughout the operating history of each design. As shown in Table 2.2, the calculated internal rod pressures for the average 8 x 8R and 17 x 17 fuel rods are significantly less than the upper-bound rod pressures calculated for the 7 x 7 and the 15 x 15 fuel rods. (However, the internal pressures of the average 7 x 7 and 15 x 15 rods will be similar to the internal pressures of the average 8 x 8R and 17 x 17 rods in Table 2.2.)

2.1.2.3 Effects of Burnup on Rod Internal Pressure

As an example of how the upper-bound rod pressures may vary with burnup, the GT2R2 calculational results were extended to a rod-average burnup of 45 MWd/kgM for the 15 x 15 PWR design (Table 2.2). A rod-average burnup of 33 MWd/kgM is equal to the current batch-average discharge burnup level for PWRs, and 45 MWd/kgM is representative of future batch-average burnup level of extended-burnup PWR fuel. The internal rod pressures of ~2400 psi, calculated for the 15 x 15 design at the higher levels, are above typical PWR system pressures of 2250 psi. However, this calculation does not imply that it is likely to operate at these pressures. Further, the calculation represents a hypothetical upper-bound pressure. The trend to extended burnup levels for future 8 x 8R and 17 x 17 fuel designs may push the rod pressures to the levels experienced by the older 7 x 7 and 15 x 15 designs at current burnup levels. Thus, extended burnups will lead to higher than currently typical internal rod pressures in all fuel.

2.1.2.4 Effects of Fuel Design and Burnup on Temperature Limits for Dry Storage

The analyses of fuel design show that there is a wide variation (upwards of a factor of ten) in internal rod pressures (and resultant cladding hoop stress) for well-populated designs and for operating conditions ranging from core average to upper bound. Even upper-bound values for PWR (15 x 15) and BWR (7 x 7) vary by almost 70% in internal pressure. This variation leads to the strong indication that a single-valued dry storage temperature limit imposes unnecessarily conservative temperature limits on lower-pressure fuel designs.

Future operation at extended burnup levels will generate higher pressures and stresses. The expected increase in cladding stress with burnup leads to another strong indication that a single-valued dry storage temperature limit, which would have to be conservative to account for future burnup levels, would unduly penalize low-burnup fuel.

2.2 VARIATIONS IN SPENT FUEL AGE AND IN STORAGE CASK DESIGN

2.2.1 Age Distribution of Spent Fuel

A substantial population of discharged Zircaloy-clad spent fuel currently exists in wet storage. The distribution of spent fuel by years after reactor discharge is shown in Table 2.3.

2.2.2 The Effect of Variations in Decay Heat Profiles Due to Fuel Age and Storage Cask Design

The heat-generation rate of spent fuel does not remain constant with time; the initial relatively rapid decay of short half-lived fission products is followed by the slower decay of the long-lived fission products. An example of this decline in heat-generation rate with increasing fuel age is given in Figure 6 of Johnson and Gilbert (1983), which shows the calculated temperature-versus-time history of PWR spent fuel assembly in a CASTOR metal storage cask. In this example, the fuel temperature decreases $\sim 280^{\circ}\text{C}$ (from $\sim 380^{\circ}\text{C}$ to $\sim 100^{\circ}\text{C}$) in the first 5 years, 50°C in the next 5 years (year 10), 25°C in the next 10 years, and only 25°C in the next 30 years. This example shows that spent fuel of different ages can cool at dramatically different cooling rates;

TABLE 2.3. Cumulative LWR Spent Fuel Inventory at All Operating Reactors in Year 1986

Age Range of Assemblies, yr	Number of Assemblies in Age Range		
	PWR	BWR	Total
1 - 5	5,761	7,728	13,489
5 - 6	1,815	2,670	4,485
6 - 7	1,475	2,859	4,334
7 - 8	1,742	2,440	4,182
8 - 9	1,927	2,568	4,495
9 - 10	1,240	2,325	3,565
10 - 11	790	1,987	2,777
11 - 12	816	1,511	2,327
12 - 13	541	1,555	2,096
13 - 14	229	487	716
14 - 15	129	120	249
15 - 16	51	59	110
16 - 17	95	0	95
17 - 18	40	0	40

No assemblies older than 18 years.

although fresh fuel can decrease hundreds of degrees celsius in the first five years, 20 year-old fuel decreases only tens of degrees in 30 to 40 years of storage

This variation in spent fuel cooling rate with age impacts the initial temperature at which the fuel can be stored. For example, a dry storage cask vendor might wish to pack a 40-yr storage cask with fuel to attain an initial storage temperature of 350°C. If the cask were packed with old fuel, it would remain near that temperature for the storage duration; and at some point well before the 40-yr storage duration, the allowable creep limits for the fuel cladding could be exceeded and the fuel could fail. Therefore, this old fuel would have to be packed for an acceptable lower initial storage temperature. Conversely, if fresh fuel were packed to attain the same 350°C initial storage temperature, it would cool down rapidly to below 200°C, a temperature at which creep rates and cumulative creep damage are acceptably low.

Added to the considerations of the effects of fuel age on spent fuel cooling rates and resulting allowable storage temperature limits is the effect of the environment within which the fuel will be stored. That environment

consists of the storage cask, whose design can vary with construction materials; the internal atmosphere and structures; and the medium within which the cask resides, be it air, a concrete structure, or a geologic repository. Moreover, the fuel may reside in its original assembly or in a consolidated form. The storage environment will impose its own heat transfer characteristics on the natural cooling rate of the fuel; and these heat transfer characteristics depend upon the specifics of the storage environment design. Therefore, the allowable storage temperature for spent fuel of a given age depends on the design of the storage environment in which it resides.

2.2.3 Effect of Fuel Age and Storage Cask Design on Temperature Limits for Dry Storage

This analysis of fuel age has shown that there is a distribution of spent fuel by fuel age; that, as the age of fuel increases, its cooling rate dramatically decreases; and that, as a consequence of the decreasing cooling rates with fuel age, significant creep deformation can occur over the storage duration if the initial storage temperature for old fuel is too high. Therefore, the variance in fuel age that exists in the population of spent fuel leads to the strong indication that a single-valued dry storage temperature limit, which would need to be conservative to account for old fuel, imposes unnecessarily conservative temperature limits on fresher fuel. Moreover, the heat transfer characteristics of the storage cask must be accounted for when considering temperature limits for the fuel residing in it.

2.3 CONCLUSIONS REGARDING THE NEED FOR MULTIPLE TEMPERATURE LIMITS

From the analyses performed in this section, it is concluded that the substantial variations in end-of-life internal rod pressures (resulting from variations in fuel design and rod burnup levels), spent fuel ages, and storage cask designs require definition of multiple-temperature limits for dry storage. The establishment of a single-temperature limit to account for these factors would impose unnecessary conservatisms, and, potentially, economic penalties for utilities and storage cask vendors. The recommended limits are presented in Section 3.0.

3.0 RECOMMENDED GENERIC TEMPERATURE LIMITS FOR DRY STORAGE

In this section, the CSFM model is reviewed. The CSFM model is then shown to produce a family of generic limit curves that represent recommended maximum initial dry storage temperatures for spent fuel. These generic temperature limit curves accommodate the variations in fuel rod cladding stress, fuel age, and dry storage cask design as described in Section 2.0. Then, the validation studies that were performed to verify that the CSFM model is technically sound and defensible are presented. These analyses involve sensitivity evaluations of the coefficients used in the CSFM model, comparisons of CSFM model predictions to recent experience in the Federal Republic of Germany, and comparison of the CSFM model to the NRC DCCG model.

The procedures for using these CSFM generic temperature limit curves to develop rod-specific temperature limits will be presented in Section 4.0.

3.1 DESCRIPTION AND METHODOLOGY OF THE CSFM MODEL

3.1.1 Review of the Methodology

The CSFM model that was used to generate the family of recommended CSFM generic limit curves described in Section 3.2 is based on a methodology that has successfully been used in predicting deformation and failure mechanisms in unirradiated and irradiated Type 316 stainless steel. In this methodology, deformation and fracture mechanisms are assigned to specific regions of stress-versus-temperature space based upon their dominance. Deformation quantities, such as strain and strain-rate, and fracture quantities, such as rupture-time, are then calculated for a specific region of the stress-versus-temperature space using the dominant mechanisms of that region. The deformation map was first suggested by Weertman and Weertman (1965) and the fracture map by Wray (1969). Nine different mechanisms of deformation and five mechanisms of fracture were used to describe Zircaloy behavior. Detailed discussions of the equations used to describe Zircaloy behavior and the map methodology are given by Chin and Madsen (1983), Tarn et al (1986), and Chin et al. (1986). In the

latter publication, models were developed to predict the behavior of irradiated material, including the recovery of irradiation damage during subsequent dry storage of spent fuel rods.^(a)

Figure 3.1 shows a typical fracture map generated by the CSFM model. The shaded area indicates the region of temperature and stress in which a fuel rod would not be expected to fail in 40 years of dry storage under isothermal-isostress conditions.

To generate a generic limit curve describing maximum initial temperature as a function of maximum initial cladding stress in dry storage, deformation and fracture equations for Zircaloy were combined with a declining temperature history (decay in the heat generation-rate of the spent fuel with time), a declining stress history (resulting from a reduction in internal rod pressure due to declining temperature and increasing fuel-cladding creep-strain), and a life fraction rule to account for damage accumulated under different stress and temperature conditions. The models for decaying temperature history, declining stress history, and the life-fraction rule are discussed in the following sections.

3.1.2 Decaying Temperature History Models

3.1.2.1 The Temperature Decay Model for 1-yr-old Fuel

To model the anticipated decaying cask storage temperature as a function of time, an analysis of the projected and measured fuel storage temperatures was made (Chin et al. 1986). The then-available data were for storage durations up to about 3.5 years. The data were found to belong to a single family of curves that has a constant slope when plotted as log temperature versus log time. By defining time as the elapsed time after discharge from the reactor, the data were expressed by the equation:

(a) During model validation, described in Section 3.3, below, typographical errors were found in two tables in Chin et al. (1986). Errata pages to that document will be disseminated, and corrections to those tables are also presented in Appendix A of this document.

FIGURE 3.1. Fracture Map Showing Region in Which a Fuel Rod Subjected to Isothermal-Isostress Conditions Would Last 40 Years or More (Chin et al. 1986)

$$T = T_0 t^{\gamma} \text{ for time } t > 1 \text{ year} \quad (3.1)$$

where T is the temperature at time t and T_0 and γ are constants, and time t is defined as the elapsed time after discharge from the reactor in years. The constant T_0 controls the initial temperature at the start of dry storage and is determined by solving for T_0 in Equation (3.1) for a temperature at the beginning of dry storage that would cause the cladding to reach its deformation and fracture limit in 40 years.

For the current report, a new model of decaying cask storage temperature was generated. This model for 1-yr fuel age was based upon projections of decaying cask storage temperature for storage durations of 50 years for two representative casks. ORIGEN-2 code calculations of decay heat were input into the COBRA-SFS heat transfer code to calculate peak fuel rod temperatures at 1-yr increments of storage time. These calculations were performed on the TN-24P and REA-2023 casks, each backfilled with helium using the approach described in Wheeler et al. (1986). Separate calculations were performed using 1-yr-old PWR and BWR fuel, each at a burnup level of 30 MWd/kgM.

These decaying storage temperature projections showed that the TN-24P casks containing the PWR fuel had the slowest cooling-rate. A slower cooling-rate results in a more conservative cladding temperature limit for dry storage, as previously explained in Section 2.2.2. Therefore, the cooling-rate projection for the TN-24P cask containing the PWR fuel was used to develop the new CSFM temperature decay history model for 1-yr-old fuel.

A log temperature versus log time plot of the TN-24 data revealed that two distinct regions, representing the decay of short- and long-lived fission products, existed. These two regions were characterized by intersecting straight lines of different slopes. The first region encompassed the storage period of 1 to 7 years after discharge; the second region, from that point through the remaining storage duration. Each region could be expressed by a best-fit line represented by an equation of the form shown in Equation (3.2) as follows:

$$T_1 = T_a \quad t^{-0.3400} \quad \text{for } 1 \leq t \leq 84 \text{ months} \quad (3.2)$$

and

$$T_2 = T_b \quad t^{-0.0840} \quad \text{for } t \geq 84 \text{ months} \quad (3.3)$$

where: $T_b = T_a \frac{t^{-0.3400}}{t^{-0.0840}}$; T_1 , T_2 , T_a , and T_b are in K; t is months after discharge from reactor; and $T_1 = T_2$ at 84 months.

Because each equation would underpredict temperatures in the region represented by the other equation (except where they intersect at 84 months), the model selects whichever of the two equations results in the higher peak rod temperature.

These equations, then, describe the decaying temperature history of the CSFM model cask containing 1-yr-old fuel PWR at 30 MWd/kgM. This CSFM model cask is a best-fit representation of the helium-backfilled TN-24P metal cask containing this same fuel. Figure 3.2 shows the excellent agreement between the decaying temperature histories for the CSFM model cask and the data for the TN-24P cask generated by the ORIGEN-2/COBRA-SFS codes.

3.1.2.2 The Temperature Decay Models for 5-, 10- and 15-yr-old Fuel

As fuel ages, its temperature decay-rate slows, as demonstrated in Figure 3.2. This requires that initial cladding temperature limits for 40-yr dry storage be lowered for older fuel, as explained in Section 2.2.2. To establish these limits for aged fuel, CSFM model casks for 5-, 10-, and 15-yr-old fuel ages were established. In these predictions, the casks were assumed to be maximally-loaded so that the initial peak-rod cladding temperature, for the respective fuel age, would cause the cladding to reach its deformation and fracture limit after a period of 40 years in dry storage. The decay heats used to generate these maximum-allowable initial temperatures were multiples of the decay heats calculated for 1-yr-old fuel at the appropriate year (i.e., 5-, 10-, or 15-yr), as presented in Section 3.1.2.1. Then, Equations (3.2) and/or (3.3) were used to represent the temperature decay histories of these model casks for 5-, 10-, and 15-yr-old fuel. These decay histories are conservative,

FIGURE 3.2. Comparison of Temperature Decay Predictions for the CSFM Model Cask for 1-yr-old Fuel to ORIGIN-2/COBRA-SFS Data for a Helium-Backfilled TN-24P Cask Containing 1-yr-old PWR Fuel with 30 MWd/kgM Burnup

i.e., they exhibit slower cooling-rates, relative to those that would be calculated by the ORIGEN-2/COBRA-SFS codes for these fuel ages because the 1-yr-old fuel decay history has a smaller temperature drop to ambient at high fuel ages than do these maximally-loaded casks.

These conservative temperature decay histories were input into the CSFM codes, and the respective initial peak-rod cladding temperature limit was adjusted until a cladding temperature limit was reached using the life fracture rule model to be described in Section 3.1.4.

These peak-rod cladding temperature limits will be shown, in Section 4.3, to be of the order of 330 to 380°C for PWR fuel (with 15-yr-old fuel having the lowest temperature limits). These temperatures are far higher than could actually be achieved by fuel having these decay heats in the TN-24P helium-backfilled cask, even with rod consolidation. For example, fuel aged 15 years in the CSFM model cask for 1-yr-old fuel has a decay heat of 0.39 kW/assembly and a peak-rod cladding temperature of 131°C. Rod consolidation would increase the cladding temperature by no more than 20 to 30°C in helium-backfilled casks, based upon examples given in Rector et al. (1986). Therefore, a margin of 170°C exists in the CSFM temperature limit for 15-yr-old fuel in the CSFM model cask. A large margin also exists for 10- and 5-yr-old fuels, 165°C and 135°C, respectively.

3.1.2.3 The Effect of Burnup on CSFM Cask Decaying Temperature History Models

The CSFM cask models for 1-, 5-, 10-, and 15-yr-old fuel were based upon 30 MWd/kgM fuel. However, fuel burnups at higher levels exist and high-burnup fuels that will generate burnups approaching 60 MWd/kgM are planned.

The major effect of higher burnups is to build in more long-lived fission products; most short-lived fission products will disappear after about 3 years. Thus, the initial heat-generation rates of 5-, 10- and 15-yr-old fuels will be about twice as high for 60 MWd/kgM burnups as that previously shown in the CSFM decay history model for 1-yr-old fuel, Section 3.1.2.1. However, the CSFM

cladding temperature limits would still include a large margin, perhaps 90°C, in the peak cladding temperature of unconsolidated 5-yr-old fuel in a model cask.

3.1.3 Declining Stress History Model

The stress decay function used to model the change in cladding stress as a function of time is a computer routine that uses the declining temperature to calculate a new stress based on a new internal pressure (Chin et al. 1986). The routine uses the ideal gas law to account for a decrease in cladding hoop stress due to reduced internal gas pressure resulting from decaying temperatures. Accumulated strain due to the effects of creep-generated increase in the cladding diameter and thinning of the cladding wall were found to be third order effects; they were not included in the calculations. The overall effect of accounting for a decaying stress under dry storage conditions is minor, with the greatest contribution coming about from the decrease in internal pressure resulting from the decaying cask temperature.

3.1.4 Life Fraction Rule Model

The effects of creep damage, for both declining temperature and stress histories, were then accumulated using a life fraction rule to assess overall damage to the cladding. This accumulation rule assumes that damage from all mechanisms are independent and additive. The life fraction rule can be expressed as:

$$1 = \frac{\Delta t_1}{\tau_1} + \frac{\Delta t_2}{\tau_2} + \frac{\Delta t_3}{\tau_3} + \dots + \frac{\Delta t_N}{\tau_N} \quad (3.4)$$

or

$$\sum_{i=1}^N \frac{\Delta t_i}{\tau_i} = 1 \quad (3.5)$$

where t_i = the time spent at the i^{th} temperature

τ_i = the time required to fracture a specimen under constant thermal and constant stress testing conditions.

When the cumulative damage fraction, the summation of the life fractions ($\Delta t_i/\tau$), reaches one, as shown in Equation (3.5), the cladding is assumed to have failed. The generic limit curve describes the temperatures, as a function of stress, at which the cumulative damage function reaches one and failure is assumed (Chin et al. 1986). This presumes defect-free cladding. Because the deformation and fracture equations are based upon a mean description of data, the limit curve implies only a 50% probability of failure.

However, the failure probability for rods on the generic limit curve is much less than 50%. This low probability results from basing recommended CSFM limits on cladding stresses generated by the peak rods in the core (see Section 4.1). Because the peak rods represent less than one percent of the rods in the core, the real failure probability of rods with dry storage temperatures and stresses lying on the generic curve is less than $50\% \times 1\%$, or 0.5% (<5 in 1,000).

Moreover, peak-fuel rod temperatures in helium-backfilled casks of the TN-24P type will be well below the CSFM limits, as described in Section 3.1.2.3; failure probabilities in this region are extremely low.

3.2 FAMILY OF CSFM GENERIC TEMPERATURE LIMIT CURVES

The CSFM model produces a family of generic temperature limit curves. These curves are generic in that they are applicable to the general population of Zircaloy-clad LWR spent fuel rods. A generic limit accounts for variations in fuel rod cladding stress and fuel age. These generic limit curves are conservative for fuel stored in helium-backfilled metal storage casks. The effects of fuel rod cladding stress level, fuel age, and storage cask design will be discussed in the following sections.

3.2.1 Effect of Fuel-Rod Cladding Stress

As described in Section 2.1, variations in fuel designs and burnups lead to wide variations in fuel rod internal pressures and cladding stresses, even when only peak rod (bounding) values are used. The CSFM model accounts for these variations. Using 5-yr-old fuel residing in the CSFM model cask (see Section 3.1.2) in analyses conducted using the CSFM model, a bounding curve was

developed that provides a generic initial temperature limit as a function of fuel rod cladding stress at the beginning of dry storage for 5-yr-old spent fuel to be stored dry for 40 years. A description of the method used to calculate this generic temperature limit curve was presented above in Section 3.1. Figures 3.3 and 3.4 are graphs that present the generic limit curve predicted by the CSFM model for 5-yr-old fuel. Initial cladding stresses between 0 and 300 MPa (representing the entire regime of allowable stresses for 40-yr dry storage) are covered in Figure 3.3. Initial cladding stresses from 0 to 160 MPa, the most relevant range, are presented in Figure 3.4. The shaded region represents allowable values of initial cladding stress and initial dry storage temperature for 5-yr-old fuel to achieve a 40-yr storage life in the CSFM model cask. The generic limit curve represents the recommended maximum initial temperatures and cladding stresses for storing 5-yr-old fuel 40 years in the CSFM model cask.

As noted in Section 3.1.4, the CSFM model defines a fuel rod failure probability of about 0.5% for rods with initial storage temperatures and cladding stresses on the CSFM generic limit curve. Those rods stored with initial temperatures and stresses below the limit curve would have even lower probabilities of failure. The generic limit in Figure 3.4 is for 5-yr-old fuel. Due to the effects of fuel age on temperature limits (previously discussed in Section 2.2 and to be expanded upon in Section 3.2.2, below), this generic limit is conservative for all spent fuel less than 5-years old.

Procedures for accounting for rod-specific cladding stress in the generation of rod-specific temperature limits will be addressed in Sections 4.1 and 4.2.

3.2.2 Effect of Fuel Age

As noted in Section 2.2, a substantial population of spent fuel has been in wet storage well beyond the 5-yr fuel age case illustrated in Figure 3.4. The decline in heat generation rate with time in dry storage is slower in older fuel. This fact suggests, as discussed in Section 2.2, a lower initial temperature limit for older fuel than for younger fuel.

FIGURE 3.3. Initial Dry Storage Temperature As a Function of Initial Cladding Stress (0 to 300 MPa) at Dry Storage Temperature for 5-yr-old Fuel

FIGURE 3.4. Initial Dry Storage Temperature As a Function of Initial Cladding Stress (0 to 160 MPa) at Dry Storage Temperature for 5-yr-old Fuel

Generic limit curves were developed from the CSFM model for 10- and 15-yr-old fuel residing in their respective CSFM model casks, as described in Section 3.1.2. General limit curves were subsequently developed for 6-, and 7-yr-old fuel to aid interpolation between the 5- and 10-yr-limit curves. These curves are presented in Figures 3.5, 3.6, 3.7, and 3.8. A comparison of these generic limits (Figure 3.9) shows that the older the fuel, the lower the CSFM temperature limit. At a stress of 80 MPa, the maximum difference in temperature limits is approximately 50°C.

Because older fuel requires lower temperature limits, it can be seen that the generic limit curve for fuel of a specified age is conservative for all younger fuel.

In Table 3.1, a listing is provided of the values of maximum temperature for initial cladding stress by which the 5-, 6-, 7-, 10-, and 15-year generic limit curves (0 to 160 MPa) were constructed.

Procedures for accounting for fuel age in the generation of rod-specific temperature limits will be addressed in Section 4.3.

3.2.3 Effect of Dry Storage Cask Design

The CSFM generic limit curves for 5-, 10- and 15-yr-old fuel are based on the temperature decay histories of the respective CSFM model casks, as described in Section 3.1.2.

These casks represent maximally-loaded, helium-backfilled metal casks containing PWR fuel. Analysis of data presented by Rector et al. (1986) suggests that the assumed peak-rod temperatures in the CSFM model casks are conservative relative to achievable temperatures, even with respect to rod-consolidated helium-backfilled casks. The temperature limits offer margins to achievable cask temperatures of from about 55°C (for 5-yr-old fuel) to about 160°C (for 10- and 15-yr-old fuel) in helium-backfilled consolidated casks. BWR fuels would have an even larger margin.

Nitrogen-backfilling of metal casks significantly increases peak-rod temperatures. Analyses of data presented in Rector et al. (1986) suggest that nitrogen backfilling would increase the peak-rod temperatures up to 80°C for an unconsolidated cask and up to approximately 160°C for a consolidated cask.

FIGURE 3.5 Initial Dry Storage Temperature as a Function of Initial Cladding Stress at Dry Storage Temperatures for 6-yr-old Fuel

FIGURE 3.6 Initial Dry Storage Temperatures as a Function of Initial Cladding Stress at Dry Storage Temperature for 7-yr-old Fuel

FIGURE 3.7. Initial Dry Storage Temperature As a Function of Initial Cladding Stress at Dry Storage Temperatures for 10-yr-old Fuel

FIGURE 3.8. Initial Dry Storage Temperature As a Function of Initial Cladding Stress at Dry Storage Temperature for 15-yr-old Fuel

FIGURE 3.9. Comparison of CSFM Generic Limit Curves for 5-, 6-, 7-, 10-yr and 15-yr Fuel Age

TABLE 3.1. Maximum Allowable Initial Storage Temperatures at Initial Cladding Stresses for 5-, 6-, 7-, 10-, and 15-yr Fuel Ages

Initial Cladding Stress, MPa	Maximum Allowable Initial Storage Temperatures at Fuel Ages (Celsius)				
	5-yr	6-yr	7-yr	10-yr	15-yr
5	509.2	487.3	455.9	447.0	436.5
10	488.8	465.5	426.4	403.0	385.6
20	465.2	415.5	380.1	372.4	366.0
30	430.4	397.0	370.1	363.8	356.5
40	408.1	389.0	363.2	385.5	350.0
50	400.6	384.0	359.7	353.1	346.5
60	395.6	380.4	355.9	349.6	343.1
70	391.9	376.5	352.5	347.0	340.0
80	388.2	375.0	350.8	345.2	337.6
90	385.7	372.0	348.8	342.8	336.1
100	380.7	369.3	346.2	341.0	333.2
110	375.2	365.9	344.6	338.0	332.1
120	370.0	362.4	339.5	334.3	328.2
130	363.5	355.2	332.2	326.6	320.0
140	355.0	346.6	324.2	318.6	312.6
150	346.9	339.1	316.5	311.2	306.0
160	339.6	331.4	310.3	304.7	299.9

A large margin would still exist in the CSFM limits for 10- and 15-yr-old fuel in the nitrogen-backfilled, unconsolidated cask; it would be approximately 90°C. For 5-yr-old fuel, however, the CSFM peak-rod limit of 380°C (see Section 4.1) would be met. For the nitrogen-backfilled consolidated cask, achievable cask temperatures would exceed the CSFM limits from about 10°C (for 10- ad 15-yr-old fuel) to about 100°C for 5-yr-old fuel.

Information was presented by Massey et al. (1986) on the NUTECH Horizontal Modular Storage (NUHOMS) system. This system incorporates a steel, helium-backfilled dry shielded canister (DSC) within a reinforced concrete horizontal storage module (HSM). The DSC is designed to contain PWR fuel of up

to 35 MWd/kgM burnup, cooled for more than 5 years, and having less than 1kW/assembly decay power. For its design ambient air temperature of 51.7°C, the maximum fuel temperature was given as 338°C. This fuel temperature suggests about a 40°C margin in the CSFM limit for 5-yr-old fuel (380°C) if the temperature decay histories of the DSC/HSM and the CSFM model cask were the same.

Data were presented in Rector et al (1986) on the monitored retrievable storage (MRS) concrete cask designed by Ralph M. Parsons Company. This cask was assumed to contain twelve canisters of consolidated rods from 15 x 15 PWR assemblies, each canister generating 1650 watts. A peak-rod temperature of 380°C was calculated. This temperature meets the CSFM peak rod limit for 5-yr-old fuel.

Procedures to account for differences in storage cask design in generating rod-specific temperature limits are addressed in Section 4.4.

3.3 VERIFICATION/VALIDATION OF THE CSFM MODEL

Validation studies were performed to verify that the CSFM model is technically sound and defensible. The results of those studies are summarized in this section; full details are provided in the appendices. The validation studies involved sensitivity evaluations of the coefficients used in the CSFM model (Section 3.3.1, below, and Appendix B), comparisons of the CSFM model predictions to recent experience in the Federal Republic of Germany (Section 3.3.2, below, and Appendix C), and comparison of the CSFM model to the NRC DCCG model (Section 3.3.3, below, and Appendix D).

3.3.1 Sensitivity Evaluations of the Coefficients Used in the CSFM Model

To substantiate the validity of the CSFM generic limit curve, an independent analysis of the sensitivities of the CSFM model to assumed material parameters and temperature decay histories was performed.

The analysis examined the sensitivity of the CSFM generic temperature limit to variations in the coefficients for the parameters that could affect creep rate and rupture time. These parameters are the grain boundary width, ductility of irradiated cladding, radiation damage annealing kinetics, and

surface energy. The results of this analysis show that CSFM generic limits are consistent with measured creep-rate data and reasonable creep-strain limits. Details are provided in Appendix B, Section B.1.

The sensitivity of the CSFM generic limit to variations in temperature decay histories was examined next. Temperature limit predictions were generated from calculations of the temperature decay histories of 5- and 15-yr-old fuel in three maximally-loaded casks using the ORIGEN-2/COBRA-SFS codes. The three casks were: the TN-24P cask, backfilled with helium and containing PWR fuel; and two REA-2023 casks, one backfilled with helium, the other with nitrogen, and both containing BWR fuel. Fuel burnups in these casks were 30 MWd/kgM. The CSFM model cask contained 30 MWd/kgM PWR fuel. Results of the analyses demonstrated that the CSFM temperature limits are conservative relative to these three casks. The limits are conservative for all casks with temperature decay rates equivalent to or greater than those of the casks temperature decay-rates. Details are provided in Appendix B, Section B.3.

3.3.2 Comparison of the CSFM Model Predictions to Experience in the Federal Republic of Germany

The validity of the CSFM generic limits was evaluated by comparing newly-available creep-rate data generated by the Federal Republic of Germany (FRG) to CSFM model predictions and by comparing temperature limits recommended by FRG to the CSFM limits. These analyses show that the FRG Zircaloy alloys exhibited higher creep-resistance at elevated temperature than assumed in the CSFM model. This higher creep-resistance is consistent with the FRG-recommended temperature limit of 450°C at a stress of 40 MPa. The recommended CSFM limit at this stress is 408°C (for 5-yr-old fuel). The discrepancy between these two limits results from the differences in the assumed magnitude of creep rates, with the CSFM model assuming faster rates. Details are provided in Appendix C.

3.3.3 Comparison of the CSFM and NRC Model Predictions

To clarify the methods used by the NRC to predict failure of fuel rod cladding during dry storage, a comparison was made between the prediction of the diffusion-controlled cavity growth (DCCG) model used by the NRC in its review of the CASTOR V/21 TSAR and the predictions of the CSFM model. The

results indicate that there are only minor differences between the predictions of the two models for cladding stresses of up to 130 MPa. The differences, both above and below 130 MPa, arise from assumptions contained in the models. These assumptions involve 1) deformation and fracture mechanisms occurring at cladding stresses greater than 130 MPa, where the CSFM model incorporates a change in mechanism that occurs in Zircaloy and the NRC model does not; 2) fuel rod decaying temperature history, where the CSFM model incorporates a continuously-decaying history and the NRC model assumes a constant temperature after 5 years; and 3) failure criterion, where the NRC and CSFM predictions concur when the NRC decohesion model is altered to allow cavitation to occur on 20% of the grain boundaries instead of only 15% (and a continuously-decaying temperature history is also used). Details are provided in Appendix D.

3.4 SUMMARY

The CSFM model produces a family of generic limit curves that describe maximum initial cladding temperature as a function of maximum initial cladding stress for a 40-yr dry storage period. To generate these limit curves, deformation and fracture equations for Zircaloy are combined with a declining temperature history, a declining stress history, and a life-fraction rule to account for damage accumulated under different stress and temperature conditions. The CSFM limits are generic in that they accommodate variations in cladding stress, fuel age, and dry storage cask design. The limits define a probability of peak-rod failure of less than 0.5%. Practically, however, fuel in many cask designs, even when rods are consolidated, will fall into temperature/stress regimes well below the limit curve, and failure probabilities would be even lower.

Validation studies that included sensitivity studies on parameters used in the model, comparisons of the model's predictions to FRG experience, and comparisons of the model to the NRC DCCG model verify that the CSFM model is technically sound and defensible. Conservatisms are built into the CSFM model because of the uncertainties inherent in extrapolating over a 40-yr period because data is lacking on the ductility of irradiated Zircaloy at the very low creep rates representative of dry storage conditions.

4.0 PROCEDURES FOR GENERATING FUEL-ROD-SPECIFIC TEMPERATURE LIMITS FOR DRY STORAGE

In Section 3.0, the recommended family of generic limit curves applicable to all Zircaloy-clad LWR spent fuel was presented. In this section, procedures are established by which these CSFM generic limit curves are used to generate fuel-rod-specific limits. First, the general method is presented, utilizing peak power rods aged 5 years as examples. Then, procedures are given to account for variations in cladding stresses for rods other than peak rods and for variations in fuel age and cask design.

4.1 FUEL-ROD-SPECIFIC LIMITS FOR PEAK POWER RODS

The general method for calculating fuel-rod-specific initial temperature limits from a CSFM generic limit curve is illustrated by calculations for the peak rods in a BWR and a PWR. It is recommended that the peak rod be used in determining rod-specific temperature limits because data for in-reactor operating conditions, which are necessary to the calculations, are readily available to the licensee from the fuel vendors' safety analysis reports for peak rods; similar data for rods operating under average conditions are not routinely available. The limits calculated using these peak rods will be conservative because they represent <1% of the rods in the core.

The method is demonstrated in the following example. For 5-yr-old PWR 15 x 15 and 5-yr-old BWR 7 x 7 spent fuel assemblies (to be stored in separate casks) having the vendor-documented peak-rod specifications given in Table 4.1 (and generated as described in Table 2.2, Section 2.0), the rod-specific temperature limit is determined as follows.

TABLE 4.1. Example Peak Power Rod In-Core Specifications

<u>Fuel Design</u>	<u>Rod-Average Burnup, MWd/kgM</u>	<u>Internal Pressure, psi</u>	<u>Hot-Volume Temperature, °C</u>	<u>O.D., in.</u>	<u>I.D., in.</u>
PWR 15 x 15	33	1707	387	0.418	0.373
BWR 7 x 7	28	1094	311	0.559	0.499

From the internal rod pressure at the end of the final reactor cycle, the corresponding known hot-volume temperature,^(a) and the dimensions of the fuel rod, the midwall hoop stress is calculated using the formula:

$$\sigma_{\text{mhoop}} = P D_{\text{mid}} / (2t) \quad (4.1)$$

where σ_{mhoop} = the midwall hoop-stress (psi)

P = the internal pressure (psi) at the hot-volume average temperature

D_{mid} = the midwall diameter (in.) accounting for cladding corrosion and

t = the cladding thickness (in.).

This hoop stress corresponds to the stress at the hot-volume average temperature. Because the maximum initial dry storage temperature is likely to differ from this value, the midwall hoop stress must be expressed as a temperature-dependent equation. Assuming ideal gas behavior of the internal rod gas, the stress at the temperature of interest, T_2 , is:

$$\sigma_{\text{mhoop}, T_2} = \sigma_{\text{mhoop}} (T_2 / T_1) \quad (4.2)$$

where $\sigma_{\text{mhoop}, T_2}$ = the midwall hoop-stress (psi) at temperature of interest T_2

σ_{mhoop} = the midwall hoop-stress (psi) given by Equation 1

T_2 = the temperature of interest (K)

T_1 = the hot-volume average temperature at which σ_{mhoop} was calculated (K).

Substituting Equation (4.1) into Equation (4.2):

$$\sigma_{\text{mhoop}, T_2} = (P D_{\text{mid}} / 2t) (T_2 / T_1) \quad (4.3)$$

(a) The hot-volume temperature is the average temperature of the available gas volume when the fuel rod is operating at full power.

Upon cooling from reactor operating temperature to dry storage temperature, the fuel pellets in the rod contract more than does the cladding, resulting in an increase in internal rod void volume. Therefore, at the dry storage temperature, the cladding stress would be less than that described by Equation (4.3). To determine the reduction in cladding stress resulting from this differential contraction, calculations were performed using the GT2R2 code, previously discussed in Section 2.1.2, for several PWR and BWR fuel designs; the LHGR was lowered to 1 kW/ft to simulate decay heat. Reductions were obtained in cladding stresses of 5% and 10% for PWR and BWR fuel designs, respectively. These values are input as a multiplier to σ_{mhoop} as shown in Equation (4.4):

$$\sigma_{mhoop,T_2} = (PD_{mid}/2t)(\alpha)(T_2/T_1), \quad (4.4)$$

where $\alpha = 0.95$ for PWR, 0.90 for BWR.

Demonstrating the above calculations for PWR 15 x 15 fuel (from Table 4.1), Equation (4.4) becomes:

$$\begin{aligned} \sigma_{mhoop,T_2} &= \frac{(1707 \text{ psi})(0.418 \text{ in.} + 0.373 \text{ in.})/2(0.95)}{2 (0.418 \text{ in.} - 0.373 \text{ in.})/2} \frac{T_2}{387C + 273} \quad (4.5) \\ &= 21.59 T_2 \text{ psi K}^{-1} \end{aligned}$$

Converting to MPa per degree K:

$$\begin{aligned} \sigma_{mhoop,T_2} &= (21.59 T_2 \text{ psi K}^{-1}) (69.894 \text{ MPa}/10,000 \text{ psi}) \quad (4.6) \\ &= 0.1509 T_2 \text{ MPa K}^{-1} \end{aligned}$$

Solving in terms of temperature T_2 :

$$T_2 = \sigma_{\text{mhoop}, T_2} / 0.1509 \text{ K MPa}^{-1} \quad (4.7)$$

This equation can then be plotted on the graph of Figure 3.4 as shown in Figure 4.1. Point A is the intersection between Equation (4.7) and the CSFM limit curve, and corresponds to a maximum allowable temperature of 382°C for the initial PWR 15 x 15 cladding stress of 98.8 MPa.

Similar calculations for the BWR 7 x 7 assembly yields the equation:

$$T_2 = \sigma_{\text{mhoop}, T_2} / 0.1039 \text{ K MPa}^{-1} \quad (4.8)$$

which, when plotted on the graph of Figure 3.4 as shown in Figure 4.1, yields a maximum initial dry storage temperature of 393°C (Point B) for an initial cladding stress of 69.2 MPa.

Figure 4.1 is based on peak-rod pressures, a spent-fuel age of five years, and the decaying temperature history used of the CSFM model cask. The effects of rod pressure, fuel age, and cask design on CSFM generic temperature limits were discussed in Section 3.2. Procedures for accounting for these effects in generating rod-specific temperature limits will be presented below, in Sections 4.2 through 4.4, respectively.

4.2 FUEL-ROD-SPECIFIC LIMITS FOR AVERAGE POWER RODS

Should the utility or cask vendor have available to it the necessary in-reactor operating data for the average power rods, rod-specific limits can be calculated for these rods. The procedures for doing so are the same as those previously presented for the peak rods in Section 4.1.

As noted in Section 2.1.2, the internal pressures of average 15 x 15 PWR and 7 x 7 BWR rods will be similar to the pressures of the average 17 x 17 PWR and 8 x 8 BWR rods for which internal rod pressures had been calculated, as shown in Table 2.2. These 17 x 17 PWR and 8 x 8 BWR rods are used in the following example of calculations of rod-specific limits for average power rods.

FIGURE 4.1. Graphical Determination of Specific Fuel-Rod Dry Storage Temperature Limit for Initial Cladding Stress at Dry Storage Temperature for Example Peak Power Rods

The temperature limits for 5-yr-old PWR 17 x 17 and 5-yr-old BWR 8 x 8R spent fuel assemblies, having the vendor-documented average-rod specifications given in Table 4.2 (generated as described in Table 2.2, Section 2.1.2), are calculated by solving Equation 4.4 to obtain an equation for T_2 (see Section 4.1 above).

For the 17 x 17 average power rod:

$$T_2 = \sigma_{mhoop, T_2} / 0.1117 \text{ K MPa}^{-1} \quad (4.9)$$

This equation is plotted on the graph of Figure 3.4 as shown in Figure 4.2. Point A, the intersection of Equation (4.9) and the CSFM limit curve, corresponds to a maximum allowable temperature of 390°C for the initial fuel rod stress of 74 MPa.

Similar calculations for the BWR 8 x 8R assembly yields the equation:

$$T_2 = \sigma_{mhoop, T_2} / 0.0112 \text{ K MPa}^{-1} \quad (4.10)$$

which, when plotted on the graph of Figure 3.4 as shown in Figure 4.2, yields a maximum initial dry storage temperature of 493°C (Point B) for an initial stress of 8.6 MPa.

A comparison of the rod-specific limits for peak and average example rods, from Figures 4.1 and 4.2, respectively, is shown in Table 4.3.

TABLE 4.2. Example Average Power Rod In-Core Specifications

<u>Fuel Design</u>	<u>Rod-Average Burnup, MWd/kgm</u>	<u>Internal Pressure, psi</u>	<u>Hot-Volume Temperature, °C</u>	<u>O.D./I.D., in.</u>	
PWR 17 x 17	33	1293	382	0.370	0.329
BWR 8 x 8R	28	142	326	0.479	0.419

FIGURE 4.2. Graphical Determination of Specific Fuel-Rod Dry Storage Temperature Limit for Initial Cladding Stress at Dry Storage Temperature for Example Average Power Rods

TABLE 4.3. Comparison of Rod-Specific Dry Storage Temperature Limits for Example Peak and Average Power Rods^(a)

		<u>Temperature Limit</u>
PWR peak		382°C (at 98.8 MPa initial cladding stress)
average		390°C (at 74.0 MPa initial cladding stress)
BWR peak		393°C (at 69.2 MPa initial cladding stress)
average		493°C (at 8.6 MPa initial cladding stress)

(a) Peak Rods: 15 x 15 PWR; 7 x 7 BWR
Average Rods: 17 x 17 PWR; 8 x 8 BWR

For the PWR rods, there is a slight (8°C) increase in the maximum allowable temperature when average rods are substituted for peak rods. This probably does not provide sufficient economic incentive, in terms of increased fuel loading temperatures, to generate the operating-condition inputs needed in the calculations should they not already be available. Only for nitrogen-backfilled unconsolidated metal casks and for MRS-type concrete casks might an incentive exist; the peak-rod temperatures in these casks containing 5-year-old fuel reach the CSFM limit (see Section 3.2.3).

In the BWR rods, there is a significant increase (100°C) in the maximum allowable temperature when average rods are substituted for peak rods. Even here, this may only provide sufficient incentive to generate operating-condition inputs to the calculations, should they not already exist, for certain casks. These casks are the nitrogen-backfilled, unconsolidated and consolidated, metal casks and the NUHOMS- and MRS-type concrete casks.

4.3 FUEL-ROD-SPECIFIC LIMITS ACCOUNTING FOR FUEL AGE

Fuel age is accounted for in generating rod-specific temperature limits by applying the procedures shown for calculating rod-specific limits at 5 years (Sections 4.1 and 4.2) to CSFM generic limit curves for the desired fuel age: Figures 3.5 through 3.8 for 6, 7, 10, and 15 years, respectively.

Using the 15 x 15 PWR peak rod case as an example of the procedure to be used, Equation (4.7) is applied to Figure 3.9 (5-, 6-, 7-, 10-, 15-yr), as shown in Figure 4.3.

FIGURE 4.3. Effect of Fuel Age on the Rod-Specific Temperature Limits for Example Peak Power PWR Rod

Because each limit curve is conservative for all younger fuel, it may be selected to calculate the rod-specific limit for fuel in the next younger age group. That is, the rod-specific limit for 15-yr-old fuel can be used for fuel ages 15 to 11 years (and younger); the rod-specific limits for 10-yr-old fuel, for fuel ages 10 to 7 years (and younger); etc.

4.4 FUEL-ROD-SPECIFIC LIMITS ACCOUNTING FOR CASK DESIGN

The fuel-rod-specific limits for 5-, 6-, 7-, 10- and 15-yr-old fuel, as described in Sections 4.1, 4.2, and 4.3, are based upon generic limits that incorporate the temperature decay histories of the CSFM model casks for those respective years. As discussed in Section 3.2.3, the CSFM model casks are conservative models for helium-backfilled metal casks, even if the rods are consolidated for all fuel ages for both PWR and BWR fuels. The model casks are conservative for nitrogen-backfilled metal casks for 10- and 15-yr-old fuel when rods are unconsolidated. By large margins, these cask designs could not be loaded to exceed the CSFM limits. By a lesser, though still significant margin, the NUHOMS DSC would not be loaded to exceed the CSFM limits for 5-yr-old fuel. Therefore, utility or cask vendors using these cask designs may simply use the procedures given in Section 4.3. No further calculations for cask-specific temperature limits would be necessary.

The peak-rod temperatures in concrete casks of the MRS design, as described in Section 3.2.3, reach the CSFM temperature limit for 5-yr-old fuel; and the peak-rod temperatures in nitrogen-backfilled, rod-consolidated metal casks would exceed the CSFM limits at all fuel ages. For these cask designs, as well as for all other designs not already discussed, including storage casks for placement in geologic repositories, cask-specific temperature decay histories would need to be modeled. Appropriate parameters from the cask-specific model would then be entered into the CSFM decay history model subroutine to generate cask-specific CSFM temperature limits for the fuel age of interest.

4.5 SUMMARY

Simple procedures were developed by which the CSFM limit curves for 5-, 6-, 7-, 10-, and 15-yr-old fuel can be used to generate fuel-rod-specific limits. Although calculations based upon peak-power rods are recommended, because of the availability of the necessary input data from fuel vendors' safety analysis reports, rod-specific limits can be calculated for average-power rods (or other specific rods) as well if similar data are available. Only for BWR rods in certain storage casks might a significant economic incentive exist to generate the data for average-power rods if they do not exist.

For those cask designs for which the CSFM model casks for 5-, 6-, 7-, 10-, and 15-yr-old fuel are conservative, no additional procedures are needed to calculate CSFM rod-specific limits. Where utilities or cask vendors wish to use other casks, the necessary parameters could be input to the CSFM decay history model subroutine.

5.0 CONCLUSIONS

The following conclusions have been generated by this study:

1. Substantial variations in end-of-life internal rod pressures and spent fuel age in the current and foreseeable U.S. spent fuel inventory, as well as variations in storage cask designs, require multiple-temperature limits for dry storage. The establishment of a single-temperature limit to account for these factors would, in some situations, impose unnecessary conservatism and, potentially, economic penalties for utilities and storage cask vendors.
2. The CSFM model produces a family of generic temperature limit curves to accommodate these variations. These limit curves establish the recommended temperature limits for 40-yr dry storage of Zircaloy-clad spent fuel.
3. The CSFM model has been verified and validated to be technically sound and defensible:
 - Sensitivity analyses have demonstrated that the model's predictions are realistic with a demonstrated measure of conservatism. In fact, the failure probability of a fuel rod whose dry storage temperature and stress is described by a point on the CSFM generic limit curve is less than 0.5% (5 in 1,000). Most stored fuel will be characterized by rods residing in the area below the limit curve, where failure probabilities are even lower.
 - Analysis of recent experience from the Federal Republic of Germany (FRG) has shown the FRG-recommended temperature limit of 450°C to be consistent with that predicted by the CSFM model when the higher creep rates used in the CSFM model are considered.
 - Comparison of the CSFM model with the NRC DCCG model shows that the differences arise from assumptions regarding deformation and fracture mechanisms occurring above 130 MPa (the CSFM model

involves a mechanism change), fuel-rod decaying-temperature history (the CSFM model uses a continuously-decaying temperature history), and failure criteria (an addition of 5% allowable cavitation to the NRC model brings the two models into close agreement when continuous-decaying temperature history is also used in the NRC model).

4. Fuel rod temperature limits for any specific fuel can be readily calculated from fuel vendor's safety analysis reports for peak rods, the CSFM generic limit curves, and procedures established in this document.
5. In terms of the issues that were originally raised in the introduction to this report (Section 1.1), we conclude the following:
 - The earlier recommendation for a single-valued temperature limit of 380°C needs to be replaced by multiple limits.
 - The CSFM methodology has been verified to be technically sound and defensible, and it has a demonstrable degree of conservatism. These characteristics should assure acceptance by the NRC for utility and cask vendor use.

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APPENDIX A

CORRECTIONS TO CHIN et al. (1986)

APPENDIX A

CORRECTIONS TO CHIN et al. (1986)^(a)

During the CSFM model verification/validation process, typographical errors were found in Tables 5 and 7 of Chin et al. (1986). Corrections are provided below, identified by vertical lines in the right margin. Errata pages are being forwarded to recipients of that report. The corrections are as follows:

TABLE 5. Fracture Equations

Theoretical Shear Strength	$\sigma_{th} = \frac{E\gamma_f}{a_o}^{1/2} \approx \frac{E}{10}$	
Transgranular Fracture	$t_f^{TG} = \epsilon_n + \left(\frac{1}{4.937}\right)\left(\frac{n}{n-1}\right)\ln \frac{0.38}{f_v^{1/2}} - 1 \dot{\epsilon}^{-1}$	
Triple-Point Cracking	$t_f^{TP} = \frac{\gamma_f}{Ed\xi} \left(\frac{\sigma}{E}\right)^{-1} \dot{\epsilon}^{-1}$	
Cavitation--Diffusional Growth	$t_f^{CD} = \frac{2.525 \times 10^{-3} \ell^3}{\delta D_{ogb} b^2 \exp \frac{-Q_{gb}}{RT}} \left(\frac{kT}{Eb}\right) \left(\frac{\sigma}{E}\right)^{-1}$	
Cavitation--Power Law Growth	$t_f^{CP} = \frac{(1 - 0.78 P_o \ell_o)}{4.87} \dot{\epsilon}^{-1}$	

(a) B. A. Chin, M. A. Khan and J. Tarn. 1986. Deformation and Fracture Map Methodology for Predicting Cladding Behavior During Dry Storage. PNL-5998, Pacific Northwest Laboratory, Richland, Washington.

TABLE 7. Reduced Fracture Equations

Transgranular Fracture	$\ln t_f^{TG} = -1.797 - \ln \dot{\epsilon}$	
Triple-Point Cracking	$\ln t_f^{TP} = -5.655 - \ln \dot{\epsilon} - \ln \frac{\sigma}{E} - \ln \frac{E}{10^4}$	
Cavitation--Diffusional Growth	$\ln t_f^{DC} = +4.15 - \ln \dot{\epsilon}_{gbs} + \ln \frac{\sigma}{E}$	
Cavitation--Power Law Growth	$\ln t_f^{CP} = -1.587 - \ln \dot{\epsilon}$	

APPENDIX B

SENSITIVITY EVALUATION OF COEFFICIENTS USED IN THE CSFM MODEL

APPENDIX B

SENSITIVITY EVALUATION OF COEFFICIENTS USED IN THE CSFM MODEL

To substantiate the validity of the CSFM generic limit curve, an independent analysis of the sensitivities of the CSFM model (Chin et al. 1986; Tarn et al. 1986) to assumed material and thermal history parameters was performed. To perform this analysis, the CSFM mechanism equations for creep rate and rupture time were independently programmed on an IBM PC using BASICA language. The PC version of the model will be referred to as CSFMPC to distinguish it from the CSFM model.

Verification of the generic limits generated by the CSFM model was performed to assure that 1) no programming errors exist that were affecting the CSFM model outputs and 2) the approximate CSFMPC model reproduced the more rigorous results of the CSFM model (Section B.1). Then, analyses were performed of the sensitivity of the CSFM model predictions to material parameters (Section B.2) and to fuel cask designs (Section B.3).

B.1 VERIFICATION/VALIDATION OF CSFM MODEL OUTPUTS OF TEMPERATURE LIMITS

The equations given in Chin et al. (1986) were used to calculate generic limits for 5-year-old fuel using the CSFMPC computations to independently verify results reported by the CSFM model. Figure B.1 demonstrates the excellent agreement between results reported by Chin et al. (1986) and this independent review. The CSFMPC results agreed with the more rigorous mainframe computer calculation to within 5°C. Typographical errors were found in Tables 5 and 7 of Chin et al. (1986) for which corrections are provided in Appendix A.

The CSFMPC temperature limits at a given stress are a few degrees higher than those produced by the CSFM model. This is primarily because, in this independent review, coarse time intervals were utilized compared to the fine time intervals used in Chin et al. (1986). In the CSFM model, time intervals are independent of storage time. Seventy-five thousand time steps are used to evaluate a storage period of forty years. In the CSFMPC model, the intervals

FIGURE B.1. Verification of CSFM Generic Limit Curve Prediction

were chosen on a logarithmic scale and increase with increasing storage time such that a total of only 45 time intervals were used ranging from 2×10^4 to 1×10^9 s.

Agreement between the independent calculations of the CSFM and CSFMPC codes verifies that the CSFM code is free of coding errors and correctly calculates creep rates, rupture times, cooling histories, kinetics for radiation damage recovery, and life fraction accumulations.

B.2 MATERIAL PARAMETER SENSITIVITY

In this section, the sensitivity of the CSFM generic temperature limit for 5-yr-old fuel to assumptions that can either increase or decrease the limit is evaluated.

B.2.1 Material Parameter Selection

The mechanism equations used in the CSFM model describe the dependence of creep rate and rupture time on specific material parameters. Selection of values for these parameters must be reasonably based on knowledge of physics, materials, and engineering. In this section, the sensitivities of the temperature limit to alternative material parameter values were determined to demonstrate that the CSFM predictions are technically sound and defensible. All material parameters were reviewed and reasonable alternative values were considered.

Measured creep-rate data and creep rupture-time data are required in the CSFM model to calibrate the mechanism equations. Measured rupture times, however, are lacking at temperatures below 400°C and stresses below 100 MPa. Calibration of the model is difficult for these low-temperature/stress conditions; and the model predictions, therefore, are principally based on the selection of material parameters used in the mechanism equations. In this study, key parameters that can affect the predicted creep-rupture lifetime were evaluated. These predictions were then related to failure limits based on an assessment of reasonable creep strain limits.

Four key parameters were identified; these are listed in Table B.1. The effects of alternative values on the CSFM temperature limits were calculated. Selection of reasonable alternative values either increased or decreased the temperature limit at a given stress. Parameters that increased temperature limits and predicted failure strains are discussed in Section B.2.2. The effect of assuming a lesser surface energy on reducing temperature limit and failure strains is discussed in Section B.3.

An increase in temperature limit would suggest that the CSFM parameter value was conservative relative to the reasonable alternative value. A decrease in temperature limit would suggest that the CSFM parameter value was nonconservative relative to the reasonable alternative value. Grain boundary width, ductility of irradiated cladding and radiation damage annealing kinetics were identified as conservative assumptions in the CSFM model (Section B.2.2); surface energy was the only parameter identified as nonconservative (Section B.2.3).

TABLE B.1. Parameters Examined in Sensitivity Analyses

<u>Parameter</u>	<u>CSFM Value</u>	<u>Sensitivity Value</u>
Grain boundary width	50 Burger's vectors	3 Burger's vectors
Ductility of irradiated cladding	10% of unirradiated value	30% of unirradiated value
Radiation damage annealing kinetics	Course network density	Fine-scale loop damage
Surface energy	35 J/m ²	2 Jm ²

B.2.2 Alternative Parameters that Increased Temperature Limits and Failure Strains

B.2.2.1 Grain Boundary Width

Rapid grain boundary diffusion, which promotes cavitation, only occurs in a narrow width comparable to the Burger's vector (Shewmon 1963). Decreasing the assumed grain boundary width from fifty Burger's vectors (50 b) to three Burger's vectors (3 b) caused large increases in the CSFM temperature limits at stresses below 80 MPa as shown in Figure B.2. The predicted increases occurred at low stresses. At these stresses cavitation is the dominant fracture mechanism. The alternative narrow grain boundary width is more reasonable than the wide width assumed in the CSFM model. In the CSFM model, the amount of cavitation is proportional to the assumed width of the grain boundary. Therefore, the wide width assumed in the model results in predictions of excessive cavitation when all other parameters in the CSFM model are assumed to be correct.

B.2.2.2 Ductility of Irradiated Cladding

Loss in tensile ductility in the range of about 15 to 40% of unirradiated ductility, depending on test temperature, has been observed in Zircaloy tubing (Yamashita and Yasuda 1984). However, there are no data on ductility of irradiated Zircaloy at the very low creep rates representative of dry storage conditions. Consequently, the magnitude of loss in creep ductility caused by irradiation is not precisely known for dry storage conditions; and, therefore, the CSFM model has assumed a decrease to 10% of the unirradiated ductility. This assumption could be very conservative. An assumed decrease to only 30% of

FIGURE B.2. Effect of Alternate Values of Theoretical and Empirical Coefficients on Dry Storage Temperature Limits

the unirradiated ductility was chosen as a reasonable, less-restrictive alternative. The predicted temperature limits for a ductility-loss to 10% (CSFM model) and 30% of the unirradiated ductility are shown in Figure B.2. Reducing the magnitude of ductility loss caused by irradiation increased the CSFM temperature limit. The increase in temperature limits was predicted at all stresses except the lowest stress (i.e., the highest temperature) examined. At the highest temperature, irradiation damage was predicted to anneal out; and, therefore, no ductility-loss would be predicted in either case.

B.2.2.3 Radiation Damage Annealing Kinetics

Experiments have demonstrated that radiation-induced loops are a major contributor to radiation effects on mechanical properties (Williams and Gilbert

1969) and that loops are much less stable (Adamson 1977) than are network dislocations (Garzarolli 1981). The CSFM model, however, assumes annealing kinetics based on cold-worked microstructures. This assumption is made to be confident that the most stable form of radiation damage is considered when modeling predicted recovery of ductility during annealing. Therefore, sensitivity to annealing kinetics was examined to evaluate the effect of assuming a more rapid, and perhaps a more reasonable, annealing rate in the prediction of temperature limits. The faster annealing kinetics for dislocation loops were assumed as an alternative to the annealing kinetics for cold-worked microstructures used in the CSFM model. As shown in Figure B.2, the assumed more rapid annealing kinetics for loops increased the temperature limit at all stresses except the low-stress/high-temperature condition, at which complete annealing was predicted for both annealing assumptions.

B.2.2.4 Combined Effects of Parameters

The combined effects of all alternative parameters that increased the predicted temperature limits are shown in Figure B.2. Assuming a narrower grain-boundary width, a lower loss in ductility caused by irradiation, and a faster annealing-rate for radiation damage collectively increased the temperature limits by 25 to 90°C. This increase in temperature limits, however, needs to be examined in the context of known deformation kinetics and strain limits for Zircaloy. The comparison described below, in subsection B.2.2.5, indicates that the alternative assumptions cannot be justified due to reasonable limits that must be applied to strain at failure.

B.2.2.5 Effects on Failure Strains

Failure strains were calculated for each assumed parameter that increased the predicted temperature limit. The predicted failure strains were compared to failure strains reasonably expected for irradiated materials. A reasonable failure-strain criterion was defined as failure strain greater than 1% but less than 4%. The lower limit of 1% failure strain is based on creep-strains in irradiated Zircaloy measured by Porsch, Fleisch, and Heits (1986). Creep-strains up to 1% were observed without evidence of failure. The upper limit of

4% failure strain is based on uniform elongation of irradiated Zircaloy cladding measured by Bauer and Lowry (1978) as a function of strain rate and temperature. Uniform elongation was found to be independent of strain rate and temperature and was about 4%. The CSFM model assumes uniform deformation of the cladding and, therefore, the 4% limit was used as a reasonable upper limit for a strain limit at failure. Even if strains greater than 4% were possible, they would probably be undesirable for poststorage rod consolidation operations where such strains would increase rod pulling forces and decrease achievable compaction ratios.

Predicted failure strains are plotted in Figure B.3 for both the alternative and CSFM model assumptions. The failure strains for the CSFM model are near 1% for all stresses. For the alternative parameters, the predicted failure strains were generally greater than 4%. Therefore, the alternative assumptions that led to a higher predicted temperature limit also imply failure strains that are higher than reasonable. It is concluded that the alternative parameters that increase the temperature limit are not justified; resultant predictions of failure strains are inconsistent with a reasonable criterion for failure-strain limits.

The predictions of failure strain shown in Figure B.3 suggest that a failure criterion based on a limit of 1% strain would result in a predicted temperature limit nearly equal to that predicted by the CSFM model based on the life-fraction rule. As shown in Figure B.4, the temperature limits are almost identical for the two criteria. General agreement between the two criteria supports the validity of the assumed rupture-lifetime limit (cumulative damage equal to one) used in the CSFM model.

B.2.3 Alternative Parameter that Decreased the Temperature Limits and Failure Strains

The only alternative material parameter assumption that reduced predicted temperature limits was the surface energy used for calculating triple-point cracking. The surface energy for triple-point cracking is assumed to be 35 J/m^2 in the CSFM model. Examination of the literature suggested that

FIGURE B.3. Effect of Alternate Values of Theoretical and Empirical Coefficients on Failure Strain

surface energies as low as 2 J/m^2 are reasonable (Yoo et al. 1983). As shown in Figure B.5, assuming this low value of surface energy reduced the temperature limit at high stresses. At these stresses, triple-point cracking is a dominant fracture mode and it is the only fracture mode dependent upon surface energy.

For the alternative value of surface energy, the predicted failure strains were generally well below 0.5% at stresses above 20 MPa. Based on a reasonable failure-strain criterion of 1% failure strain (as discussed above in Section B.2.2.5), it is concluded that the alternative choice in surface energy is not justified when all other parameters are left unchanged.

FIGURE B.4. Comparison of Maximum Initial Dry Storage Temperature Limits Computed Using a 1% Strain Limit to Predictions of the CSFM Model

B.3 THERMAL HISTORY EFFECTS

The effects of thermal history during dry storage on the CSFM temperature limits were calculated to evaluate the sensitivity of the limits to the assumed spent-fuel cask design. These calculations used the CSFM generic limit curve for 5- and 15-yr-old fuel that were presented in Section 3.2.1 of the main text rather than data from Chin et al. (1986) because a new temperature decay history model was developed. This new decay history model was based upon that of a helium-backfilled TN-24P cask, maximally-loaded with 30 MWd/kgM PWR fuel and obeying Equations (3.2) and (3.3) in Section 3.1.2.

FIGURE B.5. Effect of Assuming 2 J/m² Instead of CSFM Model Value of 35 J/m² for the Surface Energy

Three alternative thermal histories were generated from ORIGEN-2/COBRA-SFS calculations. These histories were for two REA-2023 casks, backfilled with either helium or nitrogen and maximally-loaded with 30 MWd/kgM BWR fuel, and a TN-24P cask, backfilled with helium and maximally-loaded with 30 MWd/kgM PWR fuel. Figures B.6 and B.7 compare the predicted temperature limits for these three casks and the CSFM model cask for 5-yr and 15-yr-old fuel, respectively. The CSFM temperature limits are a good fit with the other cask values at 5 years and are conservative at 15 years. The thermal history used in the CSFM model had a slower temperature-decay profile with increasing time compared with the decay histories of the other casks. Consequently, the temperature limits were higher for the other casks because they cooled faster and the fuel experienced less high-temperature creep-damage. Cladding temperatures would not

FIGURE B.6. Effect of Thermal History on Dry Storage Temperature Limits at a Fuel Age of 5 Years (The TN-24P and REA-2023 casks were artificially loaded to produce predicted cladding failure in 40 years)

actually reach these limits in these three casks; the casks were artificially loaded to produce predicted temperatures that would cause cladding failure.

These sensitivity analyses have shown that the CSFM model is conservative for the casks evaluated as well as for those casks having equivalent or faster temperature decay histories.

B.4 CONCLUSIONS

Sensitivity analyses were performed on the material parameters and temperature decay history used in the CSFM model. These analyses show that the model predictions are realistic with a demonstrated measure of conservatism.

FIGURE B.7. Effect of Thermal History on Temperature Limits at a Fuel Age of 15 Years (The TN-24P and REA-2023 casks were artificially loaded to produce predicted cladding failure in 40 years)

The material parameter values used in the CSFM model are appropriate when evaluated against a reasonable lower bound value of 1% for irradiated Zircaloy strains. Selection of these values illustrates the level of conservatism in the CSFM model predictions. Multiple mechanisms are considered in the CSFM methodology to avoid erroneously applying the predictions for conditions in which the mechanisms cannot dominate. Although it is difficult to theoretically predict creep rate and rupture time, the model provides a conservative but reasonable basis for extrapolating expected creep-rate and rupture-time data beyond the temperature and stress range of experimental data.

The results of the thermal history sensitivity studies show that the temperature limits predicted by the CSFM model are conservative for the casks evaluated. The CSFM model should, therefore, be generally applicable to predicting temperature limits for cask designs having equivalent or faster temperature decay rates.

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APPENDIX C

COMPARISON OF CSFM MODEL PREDICTIONS TO EXPERIENCE
IN THE FEDERAL REPUBLIC OF GERMANY

APPENDIX C

COMPARISON OF CSFM MODEL PREDICTIONS TO EXPERIENCE IN THE FEDERAL REPUBLIC OF GERMANY

The CSFM temperature limits for dry storage have been established based on mechanism modeling, comparison with creep-rate and creep-rupture data collected through an extensive literature review, and criteria for allowable creep strains at failure. In this section, an independent assessment of the validity of the CSFM generic limit curve is made through comparisons with experience in the Federal Republic of Germany (FRG). First, newly-available creep-rate data generated by FRG are compared with CSFM model predictions of creep rate (using the CSFM-PC model described in Appendix B); then, temperature limits recommended by FRG are compared with the CSFM limits.

C.1 COMPARISON OF CSFM MODEL PREDICTIONS TO FRG DATA

C.1.1 Creep of Unirradiated Cladding

Thermal creep rates for unirradiated cladding were calculated from plots of measured creep rates given in Peehs and Fleisch (1986) and Peehs et al. (1986). Figure C.1 shows the FRG experimental rates at 400°C, the data used to develop the CSFM model, and the CSFM model creep-rate predictions. A creep-rate of about $7 \times 10^{-11} \text{ s}^{-1}$ at 400°C, 70 MPa, and 6000 h was taken from Figure 3 of Peehs and Fleisch (1986). Similarly, a creep rate of about $1.4 \times 10^{-10} \text{ s}^{-1}$ at 395°C, 82 MPa and 6000 h was taken from Figure 1 of Peehs et al. (1986). These FRG creep rates are shown on the lower side of the distribution of data in Figure C.1. The position of the FRG data suggests that FRG cladding has greater creep resistance than that predicted by the CSFM model. The CSFM creep-rate prediction represents the best fit to the data when alpha zirconium and zirconium alloys are treated as a group; and it is conservative relative to the lower strain-rates representative of Zircaloy cladding material. Therefore, the CSFM creep-rate prediction is conservative for a generic prediction of temperature limits for Zircaloy.

FIGURE C.1. Strain Rate Data and Predictions at 400°C. The middle curve is the nominal prediction; the other two curves represent strain rates that are larger and smaller by an order of magnitude.

C.1.2 Creep Strains of Irradiated Cladding

Creep strains of FRG irradiated cladding were measured at 150 MPa and 200 MPa and at temperatures from 250 to 400°C as reported in Peehs et al. (1986). An equation was developed in Porsch et al. (1986) to represent these data. Using the CSFMPC version of the CSFM model (as described in Appendix B), creep strains were predicted to compare with the strains generated by this equation. The ratio of strain predicted by the CSFM model to the strain predicted by the FRG equation is shown in Figure C.2. A strain ratio of one would indicate agreement between the CSFM model and the FRG data. The CSFM model predicts rates similar to those predicted by the FRG equation at 300 and 325°C but overpredicts the FRG rates by up to a factor of nine at 400°C. This overprediction of the FRG data is consistent with the conservative representation of creep-rates in the CSFM model described above, in Section C.1.1.

In the FRG irradiated cladding, some of the creep-resistance may have been caused by irradiation hardening. An increased creep-resistance in FRG irradiated alloys compared to FRG unirradiated cladding, however, was not consistently observed. Therefore, it is not clear how radiation hardening could be reliably modeled for developing temperature limits. The CSFM model does not assume any improvement in creep-resistance from radiation hardening. However, the CSFM model does take into account the loss in ductility due to irradiation, as previously noted in Appendix B, Section B.2.2.2.

C.1.3 Creep Strains of Irradiated Fuel Pins

Irradiated fuel pins (Porsch et al. 1986) were exposed, in ISPRA tests, to elevated-temperature heating. The resultant strains and fuel rod pressures were measured to evaluate the temperature and stress effects on creep strain for conditions of interest to dry storage. Three temperature/stress combinations were sequentially used in the test as indicated in Table C.1.

A comparison of the creep strain predictions from the CSFM model with the FRG ISPRA data is shown in Figure C.3. The final measured creep strain was about 0.4%, whereas the CSFM-predicted creep strain was about 4.5%. The order-of-magnitude larger creep strain predicted by the CSFM model compared to the measured creep strain is consistent with the conservative representation of creep rates in the CSFM model described above, in Section C.1.1.

FIGURE C.2. Variation in the Ratio of CSFM Predicted Strain to FRG Measured Strain with Creep Duration at 150 MPa

measured creep strain is consistent with the conservative representation of creep rates in the CSFM model described above, in Section C.1.1.

TABLE C.1. Sequential Temperature/Stress Time Conditions for Irradiated Fuel Rods During the ISPRA Test (Porsch et al. 1986)

Period	Duration	Temperature, °C	Stress, MPa
1	7 months	400	36.1
2	5 months	430	41.2
3	4.5 months	450	42.5

FIGURE C.3. Comparison of CSFM-Predicted Creep-Strain to Measured Creep-Strain for the ISPRA Spent Fuel Tests

C.2 RECOMMENDED FRG TEMPERATURE LIMIT

The FRG creep results described above (Section C.1.3) led to the conclusion by the FRG investigators (Porsch et al. 1986) that a temperature limit of 450°C would be acceptable at stresss of about 40 MPa. The CSFM temperature limit would be 408°C at 40 MPa for 5-yr-old fuel.

To determine if the CSFM model was consistent with the FRG experience, the consequence of assuming a slower creep rate on the CSFM temperature limit was examined. The creep rate in the CSFM model was varied by factors ranging from one tenth to ten times its nominal value. The ratio of this assumed creep rate to the nominal rate in the CSFM model is a normalized creep rate. The effect

of varying the assumed creep rate, and therefore the normalized creep-rate, on the predicted temperature limit for a 40 MPa hoop stress is shown in Figure C.4. The CSFM methodology is consistent with the FRG proposed temperature limit of 450°C if the FRG creep rates are assumed to be about one-half the creep rates in the CSFM model.

When the assumed creep rate is decreased, a substantial increase in predicted ductility results for normalized creep rates below unity, as shown in Figure C.5. Note that the ratio of ductility predicted by the CSFM model to unirradiated ductility increases from about 0.1 to about 0.8 when the assumed normalized creep rate changes from one to one-half. This change indicates that the strong dependence of the predicted temperature limit on the assumed creep

FIGURE C.4. Sensitivity of CSFM Temperature Limit to Creep Rate at 40 MPa Cladding Hoop Stress

FIGURE C.5. Sensitivity of Predicted Ductility to Assumed Creep-Rate at 40 MPa Cladding Hoop Stress

rate shown in Figure C.4, is indirectly related to calculated annealing of radiation hardening. At a stress of 40 MPa and at temperatures near 400°C, the assumption of annealing of radiation hardening strongly influences the calculated ductility. When the assumed CSFM creep rates are reduced, the CSFM model predicts failures to occur at higher temperatures. At these higher temperatures, more annealing can occur and greater ductility is calculated. The combined effects of slower creep rate and greater ductility result in a much higher temperature limit than would be expected from the effect of a slower creep rate alone. The use of high creep rates in the CSFM model bounds the normal variability observed in Zircaloy creep rates. Thus, the use of these creep rates to generate temperature limits to cover the long duration of dry storage is believed to be conservative.

The difference between the CSFM and FRG temperature limits at approximately 40 MPa cladding hoop stress (450°C and 408°C respectively), therefore, is determined to be the result of the differences in assumed magnitudes of creep rates; the CSFM model assumes faster creep rates.

C.3 SUMMARY

Creep rates and creep strains were measured in experiments conducted by the FRG for conditions relevant to dry storage. The materials used in the tests exhibited greater creep resistance than assumed in the CSFM model. The higher creep resistance of the FRG Zircaloy cladding is consistent with a temperature limit, recommended by Porsch et al. (1986), of 450°C at stresses of about 40 MPa. The CSFM temperature limit at this stress is approximately 408°C for 5-yr-old fuel. This discrepancy results from differences assumed in the magnitude of creep rates; the CSFM model assumes faster creep rates.

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APPENDIX D

COMPARISON OF THE CSFM AND NRC MODEL PREDICTIONS

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COMPARISON OF THE CSFM AND NRC MODEL PREDICTIONS

To evaluate the methods used by the NRC to predict fuel rod cladding failure during dry storage, a comparison was made between the predictions of the diffusion-controlled cavity growth (DCCG) model used by the NRC in its reviews of the CASTOR-1C and CASTOR V/21 TSARs (U.S. NRC 1985a, 1985b) and the predictions of the CSFM model. Equations and coefficients for the NRC DCCG model were taken from Appendix A of the SER for the CASTOR-V/21 Cask (U.S. NRC 1985b).

D.1 DIFFERENCES IN PREDICTIONS OF THE CSFM AND DCCG MODELS

The method used in the DCCG model to evaluate damage to the Zircaloy cladding consists of calculating the area of decohesion at grain boundaries as a function of time in a projected cask environment. The model is based upon the work of Raj and Ashby (1975), which leads to the equations of Table D.1 after substitution of the specified coefficients.

The criterion for failure specified by the DCCG model is that the area of cladding decohesion shall be less than 15% at the end of 20 years in dry storage. This criterion is equivalent to specifying that the integral of $G(t)dt$ (Table D.1) shall be less than 0.15 after 20 years in dry storage. The DCCG model uses a decreasing cask temperature history for 5 years, after which the temperature is assumed to be constant. This conservative approach was used in the DCCG model because data reflecting measured cask temperatures after five years was not presented in the TSAR. This decaying temperature history is shown in Figure D.1. In the analyses presented in this section, the following equation was used to represent, for both the DCCG and CSFM models, the 54°C ambient air cooling curve that describes the cask temperature history in Figure D.1:

$$T(K) = 2.09 \times 10^3 (\text{months})^{-0.282} \quad (D.1)$$

TABLE D.1. NRC DCCG Model Equations and Coefficients

$$A_f = \int_0^{t_f} G(t)dt < 0.15$$

where: A_f = area fraction of decohesion that occurs over the period of time t_f .

$$G(t) = \frac{32}{3\pi} \frac{F_B^{3/2}(\alpha)}{F_V(\alpha)} \frac{\Omega \delta G_\infty(t)}{k \lambda^3} \frac{D_{gb}(t)}{T(t)}$$

$$F_B(\alpha) = \pi \sin^2 \alpha$$

$$F_V(\alpha) = 2\pi/3 (2 - 3 \cos \alpha + \cos^3 \alpha)$$

where

Quantity	Description
α =	grain boundary cavity dihedral angle 50°
Ω =	atomic volume $3.37 \times 10^{-29} \text{ m}^3/\text{atom}$
δ =	grain boundary thickness $9.69 \times 10^{-10} \text{ m}$
λ =	average cavity spacing $10 \times 10^{-6} \text{ m}$
D_{gb} =	grain boundary diffusion rate $5.9 \times 10^{-6} \exp (-131 \text{ KJ/RT}) \text{ m}^2/\text{sec}$
T =	absolute temperature K
G_∞ =	hoop stress on the cladding Pa
R =	gas constant $8.314 \text{ J mole}^{-1} \text{ K}^{-1}$
k =	Boltzman's constant $1.38 \times 10^{-23} \text{ J K}^{-1} \text{ atom}^{-1}$
t_f =	time s

FIGURE D.1. CASTOR V/21 Cask Temperature History

The time in the Equation (D.1) is the elapsed time following reactor discharge in months. The DCCG model allows for a reduction in cladding stress as a function of cladding temperature based upon the contraction of fuel-rod fission gases at the lower temperatures.

A computer program was constructed to evaluate the area fraction of decohesion as a function of initial spent fuel temperature and cladding stress. This computer program calculated the maximum initial dry storage temperature as a function of initial cladding hoop-stress at the storage temperature. The results of the calculations are shown in Figures D.2 and D.3, in which the DCCG model predictions are compared with those of the CSFM model for 5-yr-old fuel using the temperature decay history described by Equation (D.1). Values

FIGURE D.2. Comparison of the Predicted Limits for the NRC DCCG and CSFM Models (0 to 300 MPa Initial Stress)

FIGURE D.3. Comparison of the Predicted Limits for the NRC DCCG and CSFM Models (0 to 160 MPa Initial Stress)

of initial dry storage temperature as a function of initial cladding stress from 0 to 300 MPa are presented in Figure D.2; and the focus is narrowed to stresses from 0 to 160 MPa in Figure D.3.

Below fuel rod dry storage stresses of 130 MPa, the NRC DCCG model predicts a more conservative maximum dry storage temperature limit than does the CSFM model. The difference between the results of the two models is 5 to 10°C ($\sigma < 130$ MPa), depending on temperature. Above an initial fuel rod dry storage stress of 130 MPa, the NRC predictions are less conservative, predicting an allowable initial temperature of 320°C for a hoop stress of 300 MPa. This hoop stress exceeds the yield strength of the cladding at this temperature. The CSFM model deviates from the NRC DCCG model above 130 MPa to account for predicted changes in deformation and fracture mechanisms in Zircaloy that are not incorporated in the DCCG model.

D.2 EFFECTS OF CHANGES IN ASSUMPTIONS IN THE DCCG MODEL

To determine how small changes in the assumptions of the NRC DCCG model affect its predictions, calculations were performed to evaluate 1) the effect of assuming a continuously decaying temperature history (physics dictates that the temperature must continue to decay and, hence, it is unreasonable to assume a constant temperature after 5 years) and 2) the degree of decohesion that must be assumed to make the CSFM and NRC DCCG predictions agree. For these evaluations, the relevant 0 to 160 MPa initial stress range is used.

Figure D.4 shows the effect of a continually-decaying temperature history on the NRC predictions of maximum initial dry storage temperature as a function of initial rod stress. The NRC DCCG and CSFM model predictions are brought closer when continuous temperature decay is assumed in the NRC model.

Figure D.5 shows the additional effect of increasing the amount of decohesion allowed before assuming failure. By increasing the allowable decohesion in the NRC DCCG model from 15% to 20% (i.e., cavitation occurs on 20% instead of only 15% of the grain boundaries), the NRC DCCG model predictions are found to match the CSFM model predictions (when a continuously-decaying temperature history is also used in the NRC model).

FIGURE D.4. Comparison of the Predicted Limits for the NRC DCCG and CSFM Models when a Continuous Temperature Decay History is Applied to the NRC DCCG Model (0-160 MPa Initial Stress)

FIGURE D.5. Comparison of the Predicted Limits for the NRC DCCG and CSFM Models when the NRC Limit of Decohesion is Increased from 0.15 to 0.20 (0-160 MPa Initial Stress)

D.3 SUMMARY

There are only minor differences in the results predicted by the NRC and CSFM models up to an initial cladding stress level of 130 MPa. The differences, both above and below 130 MPa, arise from assumptions contained in the models. These assumptions involve deformation and fracture mechanisms occurring at cladding stresses greater than 130 MPa, decaying temperature histories, and failure criteria.

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