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Application of Critical Strain Energy Density to Predicting High-Burnup Fuel Rod Failure

Response to Comments from the Nuclear Regulatory Commission Staff

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Technical Report

Application of Critical Strain Energy Density to Predicting High-Burnup Fuel Rod Failure

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Final Report, September 2005

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PRODUCT DESCRIPTION

This report documents responses to Nuclear Regulatory Commission (NRC) staff concerning application of critical strain energy density (CSED) to predicting high-burnup fuel rod failure.

Background

Performance of high-burnup spent fuel rods under conditions that could potentially lead to cladding failure was first raised in a NRC Request for Additional Information (RAI), dated April 17, 2002. This RAI followed staff review of EPRI report 1003135, *Creep Modeling and Analysis Methodology for Spent Fuel in Dry Storage* (September 2001). Subsequently, a general approach for the structural analysis of high-burnup spent fuel under hypothetical transportation accidents was presented by EPRI at a joint Nuclear Energy Institute (NEI)/NRC meeting on October 23, 2002. Application of CSED to predicting failure of high-burnup spent fuel rods was discussed. Comments from NRC staff on using CSED as a failure measure, issued August 15, 2003, identified several topics of concern where further explanation was needed. These topics were 1) development of a stress-strain behavior and failure database; 2) computation of CSED from the stress-strain / failure data; 3) accounting for scatter in CSED data; 4) accounting for significant physical environmental effects; 5) computation of strain energy density (SED) in finite element models; and 6) validation of finite element predictions of cladding failure. Response to the comments on these topics is documented in this report.

Objectives

- To respond specifically to the August 15, 2003, NRC staff comments on CSED.
- To provide additional perspective on issues raised by NRC staff related to cladding failure behavior by using recent results from research into performance of high-burnup spent fuel under storage and transportation conditions.

Approach

Staff comments covered a wide range of issues, many of which were of direct concern to the CSED/SED methodology and database; others were peripheral issues dealing with general problems of material property testing and structural analysis. The research team's responses to individual comments are given at a level of detail judged to be appropriate to the comment and with as much information as can be derived from available sources. In so doing, it was necessary to draw on recent development work, appropriately cited in the text, which was not available at the time the comments were first issued. The response to a particular comment is given in tandem with the comment to maintain consistency between the requested information and the response.

Results

The report gives a comprehensive description of material property and failure analysis issues associated with spent fuel. In the responses to NRC staff comments, information is provided that should be of general value to readers interested in gaining wider understanding of 1) the current limitations of material property data, 2) the need for generating new data, 3) the degree of veracity of the CSED methodology to predict failure of high-burnup fuel rods under accident conditions, and 4) the analytical capabilities that exist for performing such predictions.

EPRI Perspective

Failure to resolve, in a timely manner, regulatory issues associated with interim dry storage and transportation of high-burnup spent fuel would result in severe economic penalties and in operational limitations to nuclear plant operators. NRC's Spent Fuel Project Office (SFPO) issued Revision 3 of Interim Staff Guidance 11, *Cladding Considerations for Transportation and Storage of Spent Nuclear Fuel*, in November 2003. Revision 3—as well as its predecessor, Revision 2—contain generic acceptance criteria for dry storage of spent fuel, but do not specify any such criteria for transportation of high-burnup (>45 GWd/MTU) spent fuel (EPRI report 1009276, November 2003). Proposed approaches for resolving technical issues involving potential fuel reconfiguration as a result of transportation accidents have been discussed with the SFPO. This report is EPRI's fourth in a series intended to provide responses to questions raised by NRC staff during the course of several SFPO-industry interactions. The first three EPRI reports in this series include 1009694, published in June 2004; 1009693, published in December 2004; and 1009929, published in June 2005.

Keywords

Spent fuel
High burnup
Failure criteria
Material properties
Analysis methods

ABSTRACT

The Critical Strain Energy Density (CSED) has been utilized as a failure measure for high burnup fuel rods subjected to Reactivity Insertion Accidents (RIA). A review of its application to spent fuel subjected to hypothetical transportation accidents by the Spent Fuel Projects Office, Office of Nuclear Materials Safety and Safeguards, produced a set of comments covering a wide range of issues dealing with the CSED methodology, its database and method of application to the failure prediction of high-burnup fuel rods. This report contains the responses to the NRC Staff comments, drawing upon information that had already existed at the time of review as well as development work subsequently carried out by EPRI in support of high-level waste and spent fuel management. Staff comments on CSED provided a forum to discuss the wider issue of high-burnup spent fuel requirements for material data, and to describe new analytical approaches developed by EPRI that provide alternatives to missing data. In this regard, the CSED is shown to play a critical role. First, CSED is the only single parameter that describes the three-dimensional stress-strain states that govern fuel rod failure regimes. Secondly, it was possible to avoid the limitation on empirical CSED, imposed by the lack of stress-strain data, by analytically deriving CSED criteria from available information consisting only of mechanical properties obtained from irradiated Zircaloy tubing specimens and δ-hydrides. The information developed in this report provides a comparative assessment of the state-of-the-art for failure analysis of high-burnup and low-burnup spent fuel, from which an evaluation of current needs can be made.

CONTENTS

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1 INTRODUCTION

Compliance with the provisions of 10 CFR Part 71 for the transportation of spent nuclear fuel requires that the transport package meet prescribed activity release limits, which implicitly incorporate the consequences of fuel rod failures, but without prescribing the number or nature of the failures. The prescribed activity release limits criteria are equivalent to a leak rate of 10^{-7} std cm^3/s[1] for both normal and accident conditions, for which compliance is, in principle, easier to demonstrate than activity release limits. Detectability of such a small leak rate, however, made it impractical for cask operators to comply with the regulations. A DOE source-term multi-year study conducted by Sandia National Laboratories used a detailed structural/failure-analysis-based methodology, SAND90-2406 [1], to show that this leak rate limit could be reduced by three orders of magnitude without impacting public safety, thus establishing a practical basis for compliance with the regulations. Although this was the primary objective of the DOE study, SAND90-2406 gained wider acceptance for its comprehensive analytical approach, which became the model for failure evaluation of spent fuel under hypothetical transportation accidents. Detailed modeling of the sequential interaction between the cask body, the cask internals, the fuel assemblies and the individual fuel rods, combined with failure-mode-specific failure criteria, was cast in a probabilistic framework, which enabled the calculation of fuel rod failure frequency. Utilizing the source term inventory of the fuel for the burnup levels prevailing at that time, activity release rates, and consequently the integrated leak rate, were calculated.

While the SAND90-2406 methodology is of general applicability independently of burnup level, its quantitative results are not directly transferable to high-burnup fuel[2] because of significant changes in mechanical and failure properties with burnup and the potential effects of dry storage on modifying the cladding hydride morphology. One of the critical areas in which the SAND90-2406 methodology needs to be updated is in the choice of failure criteria to replace the low-burnup criteria, which were based on cladding rupture strain and fracture toughness. To this end, the Critical Strain Energy Density (CSED), which combines the states of stresses and strains, has been proposed as a failure measure most suited for cladding with hydride-morphology-dependent failure mechanisms as can potentially exist in high-burnup spent fuel subjected to several decades of dry storage.

Activity release estimates developed in SAND90-2406 were derived as function of the number of failed rods considering all possible failure modes. As demonstrated by the SAND90-2406 methodology, there are three types of cladding failure-mode geometry possible under a

[1] ANSI N14.5 defines a reference air leak rate expressed in standard cubic centimeter per second (std cm^3/s). Through this definition, all leak rates are referred to the standard conditions of dry air at 1 atm absolute pressure and 25°C. When the leak rate is below 10^{-7} std cm^3/s for either normal or accident conditions, it is only necessary to demonstrate that the leak rate does not exceed 10^{-7} std cm^3/s [1].

[2] High burnup is defined as burnup greater than 45 GWd/MTU.

hypothetical transportation accident: (1) a short axial crack initiating under pinch-type loading at a spacer grid location, potentially extending a few centimeters axially; (2) a circumferential crack under bending-type loading, extending through the cladding wall to form a pinhole; and (3) a full guillotine break. Further, in high-burnup cladding, there is a greater possibility for multiple axial cracks to form, which could interconnect with a circumferential crack or a full guillotine break. Such a potential response regime would require detailed failure prediction utilizing failure criteria that uniquely represent high burnup cladding conditions. For example, at the end of dry storage, the cladding may have acquired tensile creep strains of up to a few percent due to creep. Under extreme thermo-mechanical history conditions, the cladding may also have acquired a mixed hydride structure consisting of radial and circumferential hydrides superimposed on pre-existing damage caused by in-service corrosion and related hydrogen absorption. A failure criterion based on strain alone would declare such a cladding to be in a failed state even before it is placed in a transport cask. This creates a technical imperative requiring new failure criteria to be developed for high-burnup spent fuel, which are more analytically sophisticated than the familiar, but simplistic, strain limit. The CSED concept evolved out of this need, and its development and utility for high-burnup spent fuel are described in two EPRI technical reports [2, 3]. Relevant material from these two reports will be brought forward as supporting information for the responses to NRC Staff comments [Appendix A] to be addressed in Section 3 of this report. First, a comparative discussion of low-burnup and high-burnup fuel rods failure characteristics is presented in Section 2, seeking to answer the question: how can a CSED-based failure criterion play a role in closing the gap between spent fuel transportation needs and the current state of material data?

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COMPARATIVE EVALUATION OF LOW-BURNUP AND HIGH-BURNUP REGIMES IN ACCIDENT ANALYSIS

The three possible failure modes predicted in the SAND90-2406 low-burnup methodology are:

- Mode I: Bending-induced transverse tearing initiating at the outer cladding surface,
- Mode II: Extension of Mode I as a breakage mode under bending, and
- Mode III: A longitudinal crack at the inner (or outer) surface caused by rod-to-rod impact

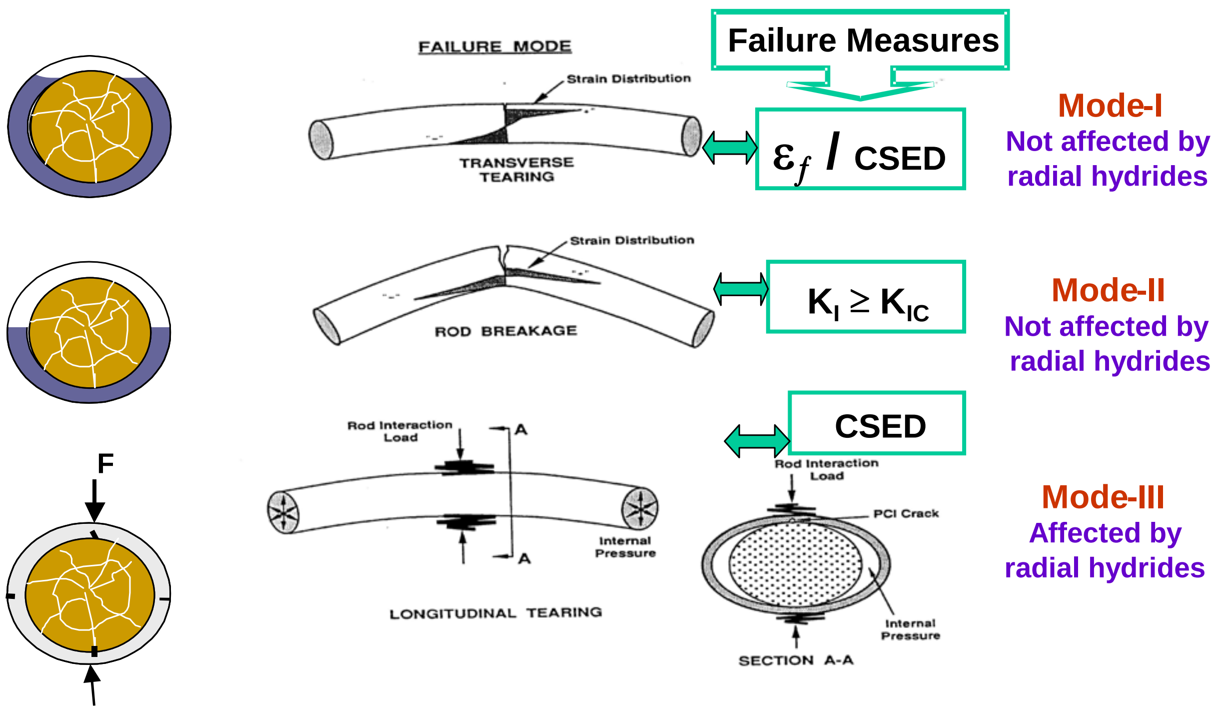


Figure 2-1
Possible Failure Modes under Cask Drop in Horizontal Orientation, Reference [1]

Failure Mode III was shown in SAND90-2406 to have the highest failure probability. The failure criteria used are a strain limit ε_f for Mode I and fracture toughness K_{IC} for Modes II and III. As already noted, the burnup level was in a range around 35 MWD/MTU. There was no consideration given to the effects of hydrogen or hydride orientation on the mechanical properties or failure limits. These differences, however, do not alter the relative ordering of failure modes for the high-burnup regime, with Mode III becoming more dominant because of the added effect of radial hydrides on cladding failure properties.

To draw further comparison between low-burnup and high-burnup cladding failure scenarios under hypothetical drop events, it is instructive to review how failure sequences were treated in the SAND90-2406 methodology. It was straightforward to define the failure sequence and the applicable failure criteria for Mode-I and -II failures. For example, initiation of Mode I based on local ductility ε_f was a logical choice. It was then assumed that, once initiated, the failure would instantaneously progress to a depth equal to the cladding thickness, potentially setting the conditions for a Mode-II failure. Thus, knowing the crack depth, it became possible to calculate a K_I value to compare to the K_{IC} from data for irradiated material, which were available mainly for Zircaloy-2 pressure tubes. Once the condition that $K_I \geq K_{IC}$ was reached, it was assumed that the crack would extend instantaneously to become a guillotine break. This assumption was conservative because, under the pure bending state of deformations in horizontal drop orientation, the crack has to extend into the compression zone of the cross section before a complete guillotine break can occur, which is analytically inadmissible.

With regards to Mode III, the process was also straightforward. For this case, it was assumed that a Mode-III failure would initiate from a pellet-cladding interaction (PCI) crack, and an initial crack depth needed to be defined. For that, a side analysis, using the EPRI fuel behavior code FREY, which was the predecessor to the FALCON code [5], was carried out to determine the maximum possible PCI crack depth before it became a through-wall crack during in-reactor service; this maximum crack depth was estimated to be 28% of cladding thickness. Using these results, it became possible to calculate, again from side analysis, a K_I value to compare to the same K_{IC} used for Mode II. In order to maximize the K_I value, it was assumed that the PCI crack was directly under the point of contact. However, K_I showed very rapid attenuation with the circumferential angle within a few degrees; this provided a basis for a probabilistic distribution for crack spacing.

The condition of spent fuel at the end of the dry storage period is now examined to assess how the approach described above may change for high burnup spent fuel. Hydrogen effects dominate changes in the mechanical properties and failure measures of high-burnup spent fuel cladding under accident conditions. The physical effects of hydrogen, which include circumferential and radial hydrides, hydride rim, and possibly hydride lenses, also affect crack initiation sites. In addition, creep during dry storage may cause the re-formation of a fuel-clad gap that could play a negative role in fuel rod resistance to accident loading. Except for the effects of fast fluence on mechanical properties, none of these conditions were considered in the SAND90-2406 methodology.

The failure measures for Modes I and II, namely, ε_f and K_{IC}, need to be modified to account for the hydride rim and higher concentration of circumferential hydrides. Creep and radial hydrides have little or no effect on these failure modes, and the effects of fast fluence will have already saturated at much lower burnup levels. Consequently, high burnup considerations should be straightforward to cope with as long as data for ε_f and K_{IC} are available for the higher levels of hydride concentrations. It is relevant to mention, however, that K_{IC} data for circumferential-radial fracture, i.e., extending perpendicular to the rolling direction, have never been generated for Zircaloy cladding at any burnup level, and the data found in the literature [6], which have been indiscriminately applied to cladding failure, were developed for through-wall cracks extending in the rolling direction. Because the direction dependence of fracture toughness is more pronounced at high hydride concentrations, this limitation on K_{IC} data has a smaller impact on low-burnup cladding. Further, radial hydrides have virtually no effect on Mode-I and -II failures, because the dominant stress acts in a direction parallel to the hydrides, unlike Mode-III failure where the highest stress is normal to the plane of the hydrides.

Thus, other than the need for updating the failure criteria for Mode-I and -II failures, failure evaluation involving these two modes would not be significantly different from the SAND90-2406 methodology. However, spacer grids in high-burnup assemblies become more compliant, i.e., less stiff, at high burnup due to the relaxation of the contact springs. This reduces the amount of fuel rod bending at the spacer grids, and although increased bending could result at the end tie plates, the higher cladding ductility in the non-fueled sections can accommodate the deformations. By transferring the Mode-I and -II failure potential to the ductile ends of the fuel assemblies, the effects of Mode-I and -II failures are further reduced relative to Mode-III failure.

Pre-existing PCI cracks were used as the initial damage condition in the low-burnup analysis of SAND90-2406 [1]. For high-burnup, the potential formation of radial hydrides during dry storage[3] would replace PCI as the stronger protagonist for failure analysis. However, unlike PCI, the available data indicate that radial hydrides do not behave like pre-existing cracks, most especially at temperatures above the hydride brittle-ductile transition temperature, ~150-200°C, even at concentration levels significantly higher than can be expected at the end of dry storage. A K_{IC}-dependent Mode-III failure criterion poses a similar situation to Mode-II failure where K_{IC} data for cladding with any amount of radial hydrides are non-existent. Qualified testing procedures for radial-axial fracture do not exist and need first to be developed for cladding type geometry, separately from the radial hydrides issue. Thus, in high-burnup fuel rods, neither the initial crack size, nor the fracture criterion can be defined for the failure mode with the highest failure probability. This requires a failure measure that does not require à priori knowledge of the crack size, which is the CSED methodology.

Another important factor in the choice of CSED as a cladding failure measure is its ability to capture the behavior of radial hydrides, which uniquely affect Mode-III failure. As the dominant failure mode, Mode III should allow for multiple crack initiation sites at critical points in the cross section, as shown in Figure 2-2. This is a different situation from the low-burnup analysis where failure initiation is tied to PCI incipient cracks, which can exist only at the ID, and become damaging only when they are located in close proximity to the impact points. CSED, applied as a spatially dependent quantity, can capture potential failures at the ID and the OD irrespective of location. The potential failure configuration in Figure 2-2, which would not exist at low burnup, at least not to equal probability as at high burnup, is postulated as a worst-case consequence of hydrogen effects.

[3] Within a given cask, the range in radial hydride concentration in the cladding may vary from nearly zero for rods with low internal pressure or low peak cladding temperature to a couple of tens of ppm at the upper range of both internal pressure and peak cladding temperature.

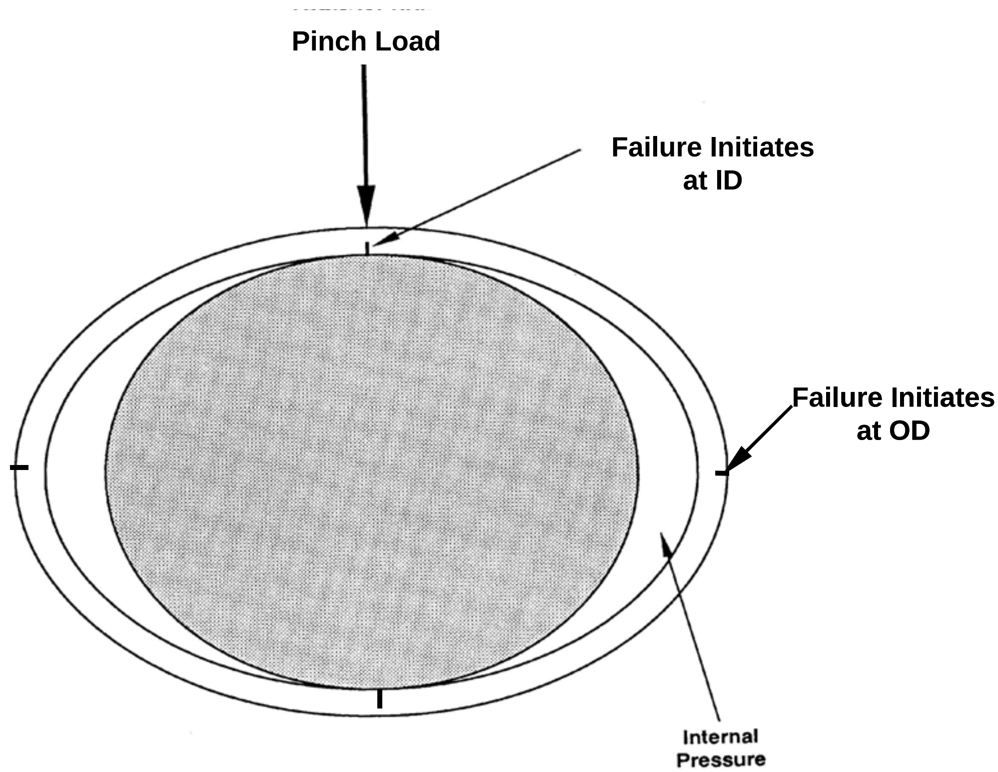


Figure 2-2
Potential Mode-III Failure Initiation Points under Pinch Loading during a Cask Side-Drop Event

Under certain operational and dry-storage conditions, high-burnup spent fuel cladding could contain PCI incipient cracks superimposed on circumferential and radial hydrides structure with synergistic interaction between all three damage forms, for which K_{IC}-dependent Mode-III failure criteria would be needed. However, qualified testing procedures for radial-axial fracture do not exist and need first to be developed for cladding-type geometry with mixed, circumferential and radial, hydride structure considering various combinations of relative concentrations. The experimental state-of-the-art lacks the methods to produce such data. Consequently, we have to rely on analytical modeling to produce such K_{IC} information, which can be done utilizing recently developed CSED-based failure criteria [2]. This CSED-based failure criteria have been analytically constructed using a damage-based constitutive model, which models the cladding as a three-phase mixture composed of Zircaloy metal matrix containing circumferential and radial hydrides of prescribed concentrations [7]. The derived failure criteria are shown in Figure 2-3, which depicts CSED as function of radial hydride concentration for given circumferential hydride contents. Applying the CSED criteria in Figure 2-3 to a finite-element-based fracture-mechanics *J-Integral* calculation for cladding geometry containing cracks of various sizes, a figure can be constructed for crack-initiation J_C as function of radial and circumferential hydrides concentrations, similar to Figure 2-3, but with J_C replacing CSED. To maintain historical consistency with SAND90-2406 methodology [1], the J_C results can be expressed in terms of fracture toughness K_{IC} using the linear unit-conversion relationship between J_C and K_{IC}.

It can be concluded from the above discussion that the adaptation of the SAND90-2406 methodology to high burnup fuel will have to involve new concepts in failure analysis, in which the CSED methodology is called upon to play a critical role. In view of the fact that failure Modes I and II are shown to have even lower effects relative to Mode-III than in the low-burnup case, the failure evaluation will mainly be concerned with Mode III.

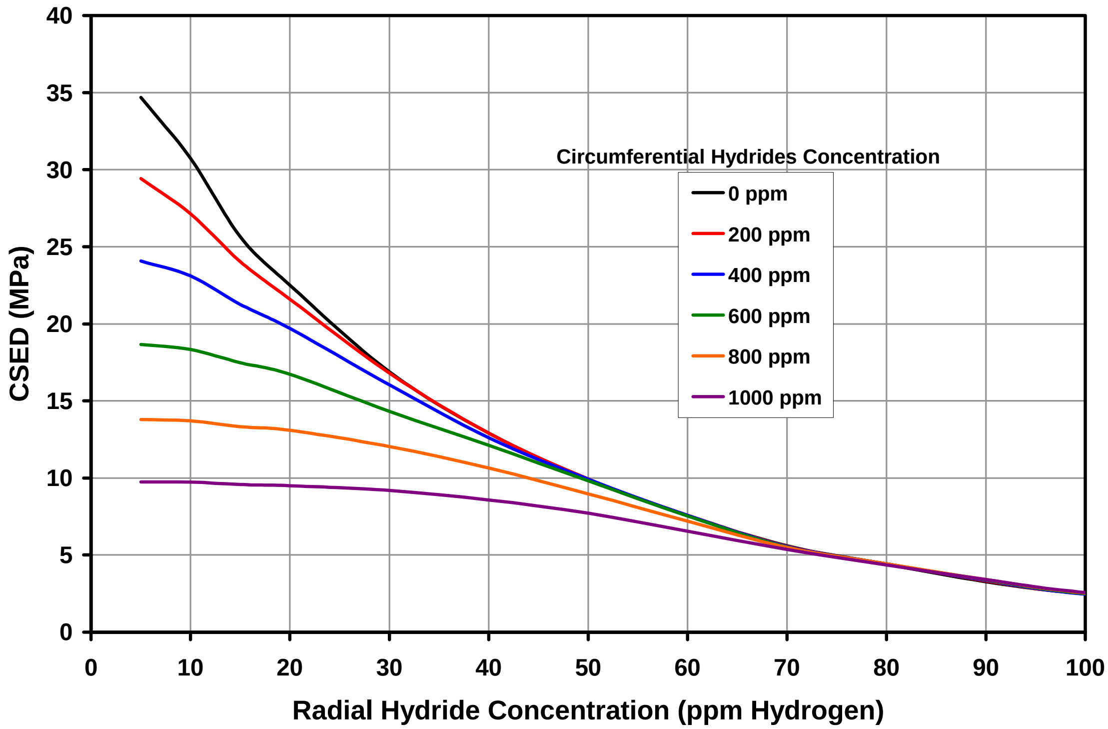


Figure 2-3
CSED at 25°C, as a Failure Criterion for Mode-III Failure, as Function of Radial Hydride Concentration for Various Circumferential Hydride Concentrations, Reference [2]

3
SPECIFIC RESPONSES TO STAFF COMMENTS

The NRC Staff comments dated August 15, 2003 (Appendix A) were divided into two categories:

- Category-1 comments that were judged to be answerable without further analytical development, and
- Category 2 comments that were judged to require further work, including modeling and analysis, to construct a response to Staff's satisfaction.

A response to Category 1 comments was prepared and conveyed to the NRC staff in September 2003 (Appendix B). The response to the Category 2 comments is the focus of this report.

In this section, the response is formatted in a way that is intended to facilitate the identification of the comment for which the response is specifically intended. Comment texts appear in italics, indented with reference to both margins, and the response to each comment directly follows the text of the comment.

After an introductory summary discussion of the CSED methodology in the NRC/SFPO letter [4], Staff's comments were stated as concerns. Responses to those concerns are given in this section; first a brief comment on the introductory discussion is provided for clarification.

The Staff's introductory summary is re-stated below:

> *A most basic and useful description of a material's strength and toughness is its uniaxial stress-strain curve. The integrated area under a stress-strain curve is the total strain energy per unit volume (strain energy density, SED) absorbed by the material at failure. When SED is associated with failure criteria, it is commonly called the critical strain energy density (CSED).*
>
> *At the joint NEI/NRC Meeting on 10/23/02, NEI proposed to use CSED as the measure most suitable for prediction of cladding failure for high burnup fuel rods. The NEI proposal (References [1] and [2]) employs a set of assumptions and a first principles approach to develop equations that relate a CSED value for any material to its corresponding fracture toughness. Using a J-integral approach, an equation is developed that relates energy release rate to the SED in unflawed cladding. From this, it is concluded that failure of cladding with a longitudinal flaw can be predicted by calculating the SED of the cladding, without modeling the flaw or performing a fracture mechanics analysis, and comparing the calculated SED to the CSED determined from material tests.*

Having established a relationship between CSED and fracture toughness parameters, the CSED data from ring tension tests, axial tension tests and burst tests are plotted against a cladding damage parameter (oxide thickness) in Figure 4 of Reference [2] (also included in Reference [1]). A best estimate curve is drawn through the data. NEI proposes that this curve (or something similar) be used as the criteria to determine fuel rod failure by comparing CSED calculated from it, to SED values computed for fuel rods from finite element impact models.

Based on the information presented, the NRC Staff has a number of concerns with respect to the application of CSED to predicting fuel rod failure. These concerns are focused on six broad areas that incorporate CSED into the methodology that will be used to predict fuel rod failure during a severe impact event.

The six areas of concern are:

1. *Development of a Stress-Strain behavior and Failure Database*
2. *Computation of CSED from the Stress-Strain/Failure Data*
3. *Accounting for Scatter in CSED Data*
4. *Accounting for Significant Physical Environmental Effects*
5. *Computation of SED in Finite Element Models*
6. *Validation of Finite Element Predictions of Cladding Failure*

Comment on Introductory Summary

The introduction provided by the Staff gives a good overall brief summary of the CSED methodology; however, on first impression it suggests an interpretation of the cited Figure 4, which may not be intended. We first must distinguish between CSED, which is an empirical correlation derived totally from material stress-strain data, Figure 4, and strain energy density as it relates to the J-integral in fracture mechanics. The statement "*Having established a relationship between CSED and fracture toughness parameters, the CSED data from ring tension tests, axial tension tests and burst tests are plotted against a cladding damage parameter (oxide thickness) in Figure 4 of Reference [2] (also included in Reference [1])*" may convey to the reader the notion that the CSED correlation in the cited Figure 4 is actually based on fracture toughness. This would not be a correct interpretation.

Development of a Stress-Strain Behavior and Failure Database

Stress-strain failure data must adequately reflect the effects of both the physical environment and damage environment to which the cladding is subjected during operation, transport and testing. The physical environment includes state of stress, temperature, strain-rate and test methods. The damage environment includes hydrogen content, hydride orientation, fast fluence and oxide thickness.

The Staff's specific concerns are focused on the effects on CSED of state of stress, strain rate, test methods and reporting of test data.

***State of Stress**: CSED is a scalar quantity and is not uniquely related to the state of stress from which it is derived. As such, there are an infinite number of states of stress that can produce the same value of CSED. Thus, axial tension, hoop tension, biaxial tension due to internal pressure, and other states of stress can each produce the same value of CSED. On-the-other-hand, because cracks, flaws and defects generally have an orientation associated with them, the failure of high burnup fuel cladding dependents directly on the state of stress in the cladding (i.e., an axially oriented crack is not susceptible to fracture from axial tension, but is from hoop tension).*

Figure 4, presented by NEI, clearly shows that state of stress significantly affects the computed value of CSED. Therefore, separate curves that adequately account for the effect of state of stress on cladding failure are necessary.

***Strain Rate**: In general, as strain rate increases, material strength increases and ductility decreases. Since both strength and ductility are directly related to CSED, it would be expected that CSED would not be as greatly affected by strain rate, as rupture strain would be. Nevertheless, uncertainty exists, and as cladding becomes more and more brittle due to high burnup, the effects of strain rate on CSED may become more severe. Testing programs currently underway will hopefully be able to address this issue.*

***Test Methods and Reporting of Test Data**: Procedures for conducting tests on fuel rod cladding and the methods for reducing and reporting data must be standardized and rigorous. Figure 4, presented by NEI, clearly shows that the CSED computed from ring tensile tests is significantly higher than the CSED computed from the axial tensile tests. Since irradiated Zircaloy cladding exhibits isotropic or near-isotropic behavior, as explained in Reference [2], one might conclude that the dramatic differences in calculated CSED are related to flaw orientation. But are they? Clearly the different values relate to the state of stress in the two types of specimens.*

Figures 5-51 and 5-58 (and Figures 5-48 and 5-55) from Reference [3] provide a clue to explain these differences. Figure 5-51 shows a stress-strain curve of a tube (axial) tensile specimen from a high burnup fuel rod. Figure 5-58 shows a stress-strain curve of a ring tensile specimen taken from the same rod at a location only a couple of inches from the tube tensile specimen. Both the tube test and the ring test are uniaxial tension tests. What is observed from these figures is that the rupture strain in the ring test is almost an order of magnitude greater than in the tube test, and that the CSED is almost six times greater. This begs the question as to whether strain in the cladding material is actually what is being represented in the ring test stress-strain curve?

Round robin ring tensile testing performed by ANL, CEA, and the Russians have shown that ring tension test data are not a good measure of material strain capability, because measured strains from this test depend on test specimen size and the test apparatus, and are not a measure of cladding strain. The ring test tends to measure artificially high strains and, therefore, artificially high CSEDs, when these erroneous strains are used. This suggests that the currently proposed CSED correlation may seriously under predict the probability of failure. More importantly, it is obvious that greater care must be exercised in the methods used to extract and report test data.

There are additional concerns that place Figure 4 data in conflict with other sources. A methodology for determining fuel rod failure during a cask impact event was developed in

Reference [4]. The NEI presentation focused attention on modifying aspects of this methodology to accommodate high burnup fuel rod cladding. However, a discrepancy exists between information contained in Reference [4] and information presented by NEI. In Figure III-30 of Reference [4], rupture strain (which is directly proportional to CSED) is plotted against the cumulative probability of cladding failure for three different ratios of hoop stress to axial stress. This plot shows that as hoop stress increases, rupture strain (and CSED) dramatically decreases. This is exactly the opposite of the trend reflected in Figure 4, which shows CSED increasing as hoop stress increases. This discrepancy needs to be resolved.

Response to Development of a Stress-Strain Behavior and Failure Database

State of Stress: It is true that different states of stress can produce the same CSED, but it is also true that each of these states of stress represents a failure state. Each data point in the CSED data says exactly that. However, the *CSED curve* is the best fit of all data points that came from separate tests, namely, axial tension, ring tension and pressurized tubes. In that sense, the CSED curve is more accurately characterized as an approximation rather than being non-unique.

It may be of interest to note that in the case of Mode-I failure, a strain-based criterion, instead of CSED, would be equally valid because of the presence of the brittle hydride rim beneath the equally brittle oxide layer. For this case, a circumferential crack at the outer surface would initiate in the elastic regime, for which the CSED criterion and the strain criterion differ only by a constant, namely, half the elastic modulus. Mode-III failure, on the other hand, represents entirely different states of stress-strain and failure characteristics because of the effect of radial hydrides. This failure mode is the primary failure mechanism for high-burnup spent fuel under transportation accidents, as discussed earlier in Section 2, being the most probable failure mode in SAND90-2406 [1]. It was the subject of recent detailed studies documented in two EPRI technical reports [2, 3], in which Mode-III-specific failure criteria were analytically developed in the form of CSED as function of circumferential and radial hydride concentrations. This analytical CSED model was derived using damage-mechanics formulation for a cladding constitutive model that treats the cladding as a three-phase composite consisting of circumferential hydrides phase, radial hydride phase and Zircaloy metal matrix. The model utilizes the mechanical properties of irradiated Zircaloy with no hydrogen effects for the metal phase and the mechanical properties of δ-hydrides for the hydride platelets phase. The failure criteria in the model are based on individual phase failure properties consisting of two parameters for each of the metal and the hydride phases: the strain to initiate damage and the strain to complete the damage to failure. In this way, the model is capable of simulating the stress-strain curves for high-burnup cladding with mixed hydride structure as function of fast fluence and the concentrations of circumferential and radial hydrides. Since the mechanical properties of the individual phases, i.e., irradiated Zircaloy and δ-hydrides are more accurately known than the metal/hydride mixture as a single material, the CSED derived from the model would be more accurate than the empirical correlation derived from data. The model is used as a tool to provide CSED information over a wide range of circumferential/radial hydride concentrations. In effect, the model serves as an analytical test apparatus for generating stress-strain curves for cladding conditions for which no actual data exist. Detailed description of the damage-based, three-phase constitutive model is given in Reference [7]. The analytically derived CSED is shown in Figure 3-1, which depicts CSED as function of the circumferential hydride concentration as the primary variable and the radial hydride concentration as a parameter. As can be seen in this figure, strong synergism exists between radial and circumferential hydrides for radial hydrides concentration levels below 70 ppm. At higher concentration levels, the CSED is totally governed by the radial hydrides. This behavior can be seen more clearly in Figure 2-3, which depicts the same information, with radial hydride concentration as the primary variable.

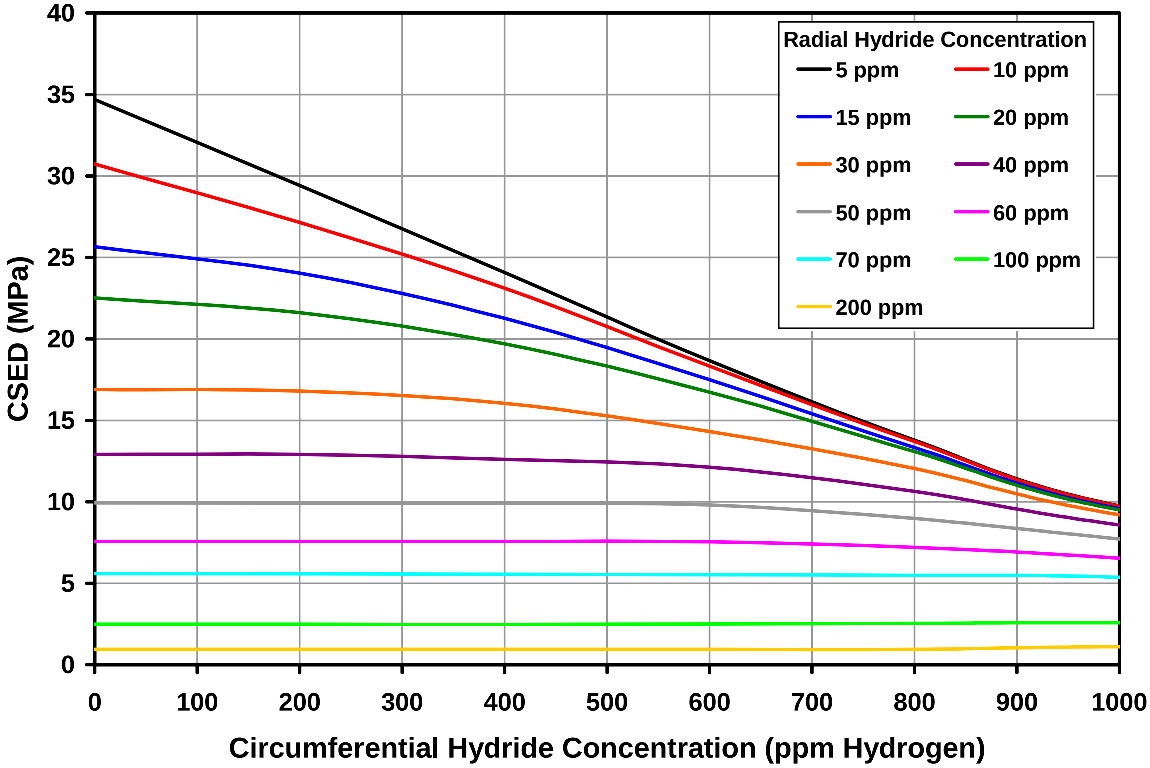


Figure 3-1
CSED at 25°C, as a Failure Criterion for Mode-III Failure, versus Circumferential Hydrides for Various Radial Hydride Concentrations, Reference [2]

The suggestion that partitioning CSED into direction-specific curves that can better explain failures may be applicable in very special situations where the contribution of stress-strain states in the other directions can be totally neglected, a condition that would not exist in fuel rods. Consider, for example, reactivity initiated accidents (RIA), to which the CSED methodology was first applied. Under RIA, the cladding experiences three-dimensional states of stress induced by the pellet-cladding mechanical interaction (PCMI) loading, namely, small radial compression, large hoop tension and equally large axial tension. These loading conditions are individually simulated by the mechanical properties tests used to quantify CSED, namely, axial tension, ring tension and closed-tube burst tests, and the best-fit curve becomes a model that represents the aggregate effects of the fuel-rod's multi-axial state of stress. The best-fit curve from the figure cited in the comment is reproduced, along with other information, in Figure 3-2 below.

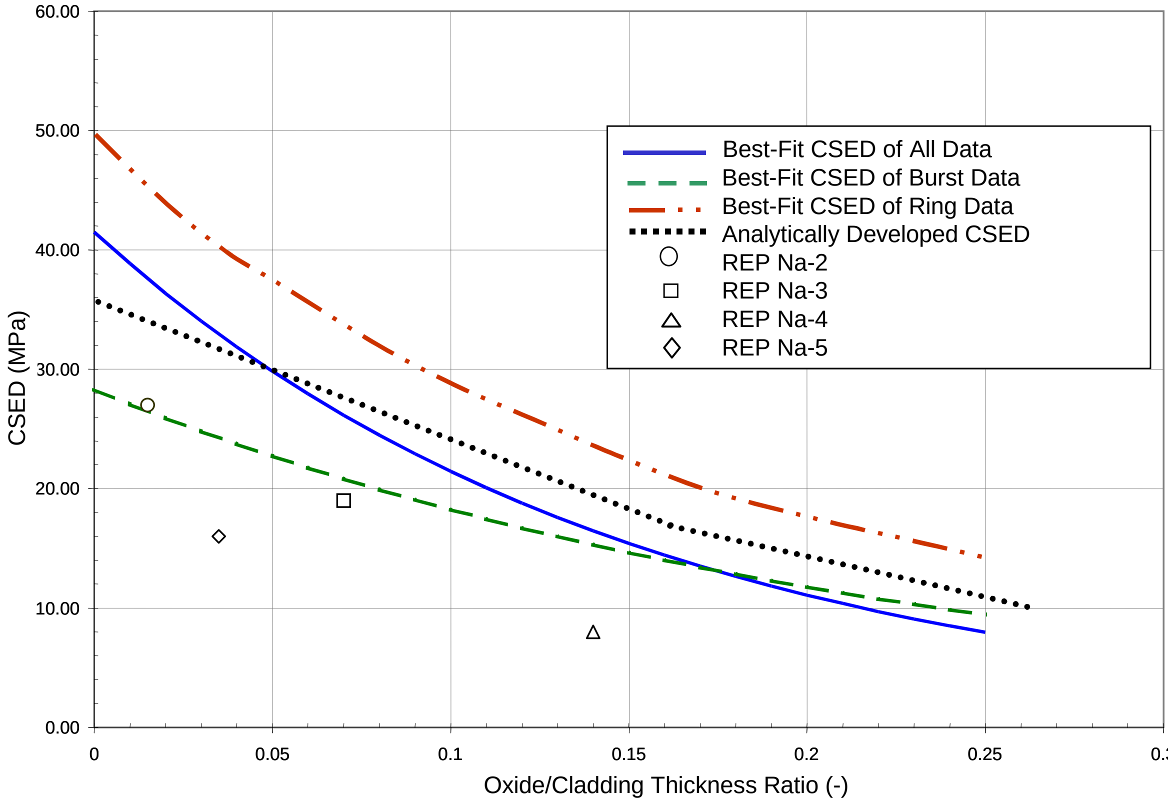


Figure 3-2
Comparison of FALCON Calculated SED for the CABRI REP Na Tests with the CSED Curves Developed from Non-Spalled Test Data for All Data, Tube Burst Data, Ring Data and Analytically Developed CSED

The following observations on Figure 3-2 provide support to the original premise that the best-fit CSED curve is the appropriate failure measure:

a. The analytically derived CSED curve [2], which is totally independent of the CSED database, envelops the best-fit empirical CSED curve in a remarkably close fashion. This is a first-of-a-kind analytical model for CSED that validates the empirical CSED best-fit correlation.

b. Applying the burst-data CSED to RIA, based on the assumption that failure would be driven by the hoop stress is shown to be too conservative. As is illustrated in this figure, one, and possibly two, of the RIA CABRI tests [8] would have been predicted to fail had the CSED curve based on burst data alone been used.

c. By virtue of points (a) and (b) above, the best-fit curve exhibits a more realistic material behavior than any individual data point or group of points in the database; it plays the role of a filter for the experimental bias in the data, such as observed in the ring tension data in the figure.

The mode of failure under PCMI forces in an RIA transient is similar to Mode-III failure induced by pinch forces in a transportation accident. Now, can the arguments related to the application of CSED methodology to RIA be extended to cladding under hypothetical transportation accidents?

The answer is “yes”. In a transportation accident, the cladding cross section is subjected to a knife-edge longitudinal impact load superimposed on pellet reaction forces and internal gas pressure. These forces induce a complex three-dimensional stress-strain state dominated by the hoop stress similar to PCMI under RIA transients, for which the best-fit CSED proved to be the appropriate criterion. Similarly, under Mode-I failure, the cladding outer-surface is subjected to axial tension due to longitudinal bending and circumferential tension due to the ovalization of the cross-section under the interaction forces with the fuel pellets. Applying separate direction-specific CSED curves as suggested in the comment is not only intractable, but also would involve much greater uncertainty than applying a single best-fit curve that represent contributions from all stress/strain components.

Directional dependence of the failure geometry is automatically reflected in the calculated value of the SED. For example, if the hoop stress is the dominant loading such that an axial fracture could develop, to the exclusion of other orientations, then the hoop stress-strain state would have the major contribution to the calculated SED. By comparing the three-dimensional SED to the one-dimensional CSED, one might consider that the method is conservative; however, the fundamental principle of the SED/CSED methodology is that the energy capacity of the material is an invariant quantity for a given internal physical state of the material as characterized by the hydride structure and associated internal damage. Therefore, if a flaw of certain orientation exists as a result of service conditions, such as an incipient PCI crack or a hydride lens caused by OD oxide spallation, extension of such a flaw to failure would occur under the effect of a three-dimensional state of stress. The calculated far-field SED at failure, to be compared to CSED, would reflect the entire near-field three-dimensional stress state. There is no one-to-one correspondence between the CSED individual components and those of the near-field SED. Thus, the portioning of SED/CSED would not be compatible with actual material response.

Strain Rate: As noted in the comment, CSED is not sensitive to the loading rates anticipated. The rupture strain decreases with increasing strain rate, but the strength increases with increasing strain rate, and the net effect is that the area under the stress-strain curve, hence the CSED, remains virtually unchanged. As the material becomes more and more brittle, strength and ductility become difficult to measure at high loading rates, which could produce an apparent effect of “strain-rate” dependence. Therefore, when considering strain-rate, or more accurately loading-rate, effects on material behavior, one should consider the entire stress strain curve.

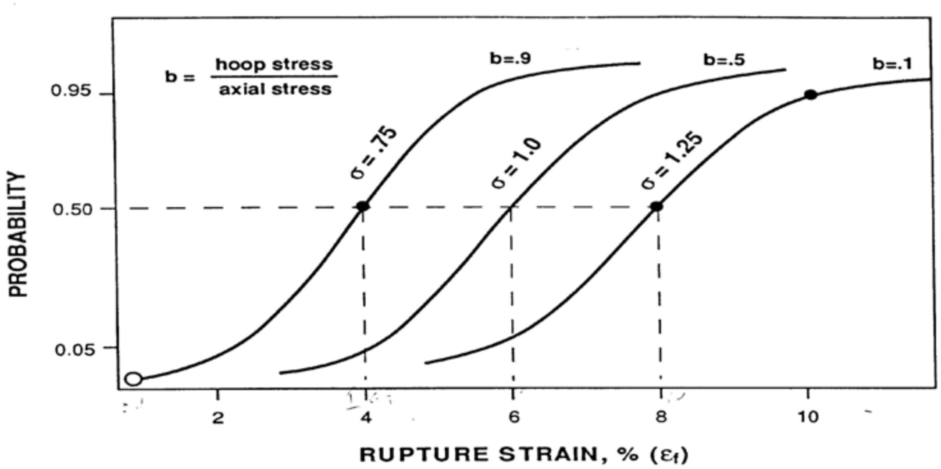
The material’s ability to respond to high loading rates is dependent upon the material’s internal wave speed, ($\sqrt{E/\rho}$, where E is the elastic modulus and ρ is the mass density), compared to the applied loading rate. Material property data are usually generated for strain rates of the order of 10^{-3} per second, which is roughly compatible with the material’s internal response rate. Significant differences in material response begin to show up around 10^{1} - 10^{2}/s. Experience with cask drop tests on concrete targets show that the cask’s impulse momentum reaches its maximum value in about 3-4 milliseconds. Fuel rod’s maximum response would be delayed by at least several milliseconds, which brings the time it takes the cladding strain to reach 1%, say, about 10 milliseconds (see Figure 2-12 of Reference [3]), indicating a strain rate of about 1/s. This is a factor of 10 smaller than the range of high strain rate effects on ductility. In accident analysis, however, the larger concern is not the strain-rate effect on material ductility, but rather the effect of the dynamic amplification of the loading. This brings in the question of whether realistic targets versus unyielding target should be considered in the analysis. This question is addressed in a recent EPRI Technical report, Reference [3].

Test Methods and Reporting of Test Data: Cladding "Mechanical property" data in the available literature are derived from three types of tests: (a) axial tension tests using tube or dog-bone specimens; (b) ring tension tests utilizing notched and un-notched specimens; and (c) open-tube and closed-tube burst tests. All three types of tests are in reality structural tests, i.e., they all activate three-dimensional stress-strain states in the specimen to varying degrees, with at least one direction being dominant, but the measurements are made only in the dominant direction in terms of forces and displacements. These are then transformed into stress-strain curves either by hand calculations or through finite element modeling of the specimens and their interaction with the loading fixtures. The CSED is then calculated as the area under the one-dimensional true-stress vs. true-strain curve analytically interpreted for the dominant stress direction. This method suffers from inherent inaccuracies for two reasons: the measured strain depends upon a gage length and the stress is inferred from the force through modeling and analysis. The measured strain becomes gradually inaccurate for deformations beyond the uniform elongation, and at specimen rupture, the total elongation strain (the true strain at rupture) is accurate only if determined from reduction-in-area measurements. These inaccuracies are unavoidable and can be minimized, but not totally eliminated, through standardized testing procedures. However, much of cladding property data that are available in the nuclear industry do not conform to standardized procedures. Therefore, in the absence of such standardized procedures, we are in fact replacing what might be described as "standardized inaccuracies", with "non-standardized inaccuracies".

Some of the modeling and analyses used to derive the stress strain curve are quite sophisticated, which would ensure self-consistency between the inferred stress and the measured strain. This means that utilizing both quantities, in the form of CSED, would provide a higher level of accuracy than using a single quantity, namely strain, as a failure criterion. This perceived "accuracy" has been one of the primary motivations for adopting the CSED as a failure measure. Secondly, the energy capacity as an intrinsic property of the material is the basic ingredient in the formulation of damage and fracture mechanics theories. Third, as already mentioned, for the loading regimes of interest, the CSED is strain-rate insensitive because the shape of the true stress-strain curve changes with strain rate in such a way that the area under the curve remains virtually the same. Fourth, a one-dimensional failure measure, e.g., strain, would not be compatible with the generally three-dimensional stress-strain state of the cladding under service and accident loadings, which makes the CSED/SED methodology necessary.

The above discussion lends perspective to the observations made in the review comment. The points raised regarding the data in the cited Reference [3] and the inconsistent behavior of axial tension and ring tension tests deal with the well-known difficulties associated with material properties testing of highly irradiated cladding. The design of uniaxial test specimens and ring tension specimens differ from traditional uniaxial tension specimens. As a result, two main problems arise. For the axial tension tests, the gage section is not well defined, and test specimen can be either a full tube or a notched section, with different gage lengths, which may explain why ring tension tests give higher measurements. For the ring tension, the double-D loading method can introduce bending within the gauge section. The amount of bending is a function of the tolerances between the loading inserts and the sample, and can affect the measured elongation values. Also, the gauge section, as in the axial tension test specimens, is not well defined. This also can introduce uncertainties in the total elongation strains measured in the post-test examinations. These difficulties may explain why the earlier round robin ring tension tests at ANL and CEA have been disqualified. Since then, improvements have been introduced in the design of the test fixture. It should be mentioned, however, that ring tension data are needed, and improved ring tension testing procedures, coupled with finite element analyses, have been developed at both laboratories. These issues pertain to material data development in general, and are not specific to CSED.

The Staff comment in the last paragraph points to a possible conflict between CSED and the failure criteria in SAND90-2406 report [1], Figure III-30, reproduced here as Figure 3-3. As will be explained below, there is no such conflict, but rather a misinterpretation of some of the information included in the figure, perhaps caused by inadequate description of the figure. Figure 3-3 describes how the effect of biaxiality (ratio of hoop stress to axial stress) on material ductility is treated in the SAND90-2406 methodology. It appears that an increase in biaxiality was misinterpreted to mean an increase in the hoop stress. The explanation below should further clarify the problem.



● - Experimentally based data points used to develop rupture strain failure curves

○ - Failure probability defined as 1 x 10^{-6} at elastic limit strain = 0.8%

PROBABILITY OF FAILURE FOR STRAIN, x

$$F(x) = \frac{1}{\sigma\sqrt{2\pi}} \int_{-\infty}^{x} \exp\left(\frac{-(y-\mu)^2}{2\sigma^2}\right) dy$$

μ = Mean Value of Distribution

σ = Standard Deviation of Distribution

Figure 3-3
Probability Distribution for Cladding Rupture Strain as Function of Biaxiality Ratio, Reference [1]

As the biaxiality ratio, *b* in Figure 3-3, increases, the ductility decreases; this is a well known material behavior. Now, consider a case where the hoop stress is at a fixed value, say 600 MPa, but the axial stress varies from 60 MPa ($b = 0.1$) to 300 MPa ($b = 0.5$) and 540 MPa ($b = 0.9$). Figure 3-3 says that the rupture hoop strain would vary from 8% to 6% and 4% for the three biaxiality ratios of 0.1, 0.5 and 0.9, respectively. Extending the analogy to high-burnup cladding, the CSED best-fit curve referred to in the Staff comment, which is also shown in Figure 3-2, should apply to all three cases. The response quantity that should be compared to the best-fit CSED is the summation of SED contributions from all active stress components, in this example: hoop and axial. This multi-dimensional aspect of CSED does not often come to mind when it is compared to the one-dimensional strain-based criteria, which may have contributed to the misunderstanding in the Staff review comment.

As a final remark, it should be noted that since the initial development of CSED, more data became available and much of this data was generated with CSED in mind. However, as noted earlier in this response and in Section 2, application of the CSED methodology to the analysis of hypothetical transportation accidents will not be by direct transfer of the present CSED data, but rather by meticulous adaptation of the methodology to the specific requirement of the failure modes expected, which led to the analytically-developed CSED failure criteria discussed earlier and presented in Reference [2].

Computation of CSED from the Stress-Strain/Failure Data

The concerns enumerated above lead directly to the concern for how CSED is calculated from test data.

- *What is the precise methodology used to calculate CSED in the tube tensile test, the ring tensile test, the burst test and the bending test?*
- *What does the calculated CSED actually represent, and is it a true expression of the SED in the cladding alone?*
- *What is the volume over which CSED is calculated? Is it measured at a point, or, is it measured over a smeared volume by computing the total elongation over some gage length, as would be expected in the development of a typical stress-strain curve?*

These questions are related to how the calculated CSED is actually used in failure prediction for cladding and is discussed again in Item 5 below.

Response to Computation of CSED from the Stress-Strain/Failure Data

The process of constructing the CSED from stress-strain data used the information reported by the testing laboratory, which consisted of yield strength, ultimate strength, uniform elongation and total elongation. Adding elastic modulus from the literature, a piecewise continuous stress-strain curve was constructed, which was then integrated to obtain the CSED. Biaxiality adjustment factors were applied to the uni-axial data to allow the development of a single correlation and to make the whole data set applicable to the multi-axial stress states experienced by the cladding in an RIA transient. The methodology of calculating the CSED is described in Reference [8], which is similar to the source cited in the Staff comment as Reference [2]. Incidentally, there are no bending tests in the CSED database.

The CSED is the *C*ritical *S*train *E*nergy *D*ensity, which represents the maximum energy per unit volume the material can resist. SED, is the *S*train *E*nergy *D*ensity delivered to the component during the loading event, for example, control rod ejection, and is computed in the finite element analysis code as function of spatial position, namely at the element integration point. Failure of the cladding is indicated when the maximum value of SED at any spatial position in the cladding reaches a value equal to CSED.

Since the CSED is the area under the stress-strain curve, it is the property of a material point and has the units of $J/m^3 = N\text{-}m/m^3 = N/m^2 = Pa$. In British units, it is $lb\text{-}in/in^3 = lb/in^2 = psi$.

Accounting for Scatter in CSED Data

There is significant scatter in the data presented by NEI correlating CSED with the cladding damage parameter (oxide thickness). The single, best-fit curve drawn through the data presented in Figure 4 does not adequately describe the failure probability of high burnup fuel cladding because of the large uncertainty in any estimate of CSED. Best estimate and lower bound (mean minus one standard deviation, or 84% non-exceedance probability) curves need to be constructed to quantify uncertainties in order that a probabilistic fuel rod failure evaluation can be performed, as intended in Reference [4].

[Comment: The CSED in Figure 4 appears to be based on total elongation strains at failure as opposed to uniform elongation strains. Use of uniform elongation strains from burst and axial tensile tests typically reduces the scatter in the CSED data by a factor of 3 to 4 as compared to use of total elongation strains from the burst and axial tensile tests. However, using uniform elongation strains will generally under predict CSED.]

Response to Accounting for Scatter in CSED Data

The mechanical properties of irradiated cladding are measured using testing methods that inherently introduce some amount of data scatter. Test specimen designs do not conform to standardized procedures in all cases. As a result, problems arise in the ring tension and axial tension tests, as already mentioned, and burst tests are structural tests that have to be interpreted analytically, especially if they contain major localized hydride damage.

Additional sources of scatter include such factors as specimen design and fabrication, loading and heating conditions, measurement uncertainties, and material variability arising from differences in material fabrication (composition and heat treatment) and cladding geometry (wall thickness and outer diameter). Irradiation temperature and hydrogen concentration also contribute to the scatter in the measured material strength and elongation values.

Irradiation further complicates the cladding mechanical properties because of the dependence of irradiation damage on the operating temperature. For example, samples removed from the upper regions of the rod can have different irradiation hardening characteristics compared to the lower region of the rod because of the higher cladding temperature. These uncertainties are also inherent in the data and are difficult to account for in any modeling activity.

Thus, because of the nature of irradiated cladding, data scatter will always be present in the mechanical properties reported. This must be recognized when developing material models for irradiated Zircaloy material. In deterministic analysis, the accepted practice in material model development is to use the best fit of the data so as not to bias the model's applications. Probabilistic evaluation, however, requires statistical analysis of the data, which automatically takes uncertainties into consideration.

A statistical analysis of the CSED database will be performed as part of the overall probabilistic failure analysis methodology for hypothetical transportation accidents, and will be addressed at the time when such a probabilistic evaluation is performed for transportation accidents.

Accounting for Significant Physical Environmental Effects

> *The failure mechanisms for fracture have sometimes proven to be too numerous and complex to trust the accuracy of empirical correlations, other than over narrow ranges of materials and test conditions prototypical of the intended application. For example, in the case of ferritic steels the existence of a transition temperature for ductile versus brittle fracture has complicated the efforts to develop correlations. In the case of zirconium alloys the complicating effects of temperature are further compounded by the effects of hydride orientation and total content on fracture toughness.*
>
> *Indeed, further complexity is encountered as the state of damage in the cladding becomes more severe due to high burnup. In these circumstances it is important to develop empirical correlations within ranges of variables that more precisely reflect the physical conditions (state of stress, strain rate, etc.) to which the cladding is subjected. If cladding is subjected to axial tension at high strain rates, a correlation between CSED and the damage parameter for those specific physical conditions will produce far more meaningful results than a correlation between CSED and the damage parameter that includes all states of stress and low strain rates. Clear delineation among states of stress is necessary to develop better correlations with failure data and to minimize uncertainty.*

Response to Accounting for Significant Physical Environmental Effects

The points raised in the above comment are valid in general and are especially relevant for the cladding loading that could exist during transportation accidents. However, before discussing transportation-specific issues, it is important to mention that the CSED criterion was developed for application to RIA, for which the failure mode is an axial crack under PCMI loading. As previously mentioned, this type of loading, which is driven by the radial and axial expansion of very hot fuel pellets against relatively cold cladding, produces stresses and strains in the hoop and axial directions that are of almost equal intensity. The data used in developing the CSED curve involve similar stress-strain states, and therefore were appropriately utilized.

With regards to transportation accident analysis, it is necessary to recognize the effects of hydride morphology on cladding failure modes and to develop failure measures (criteria) that are failure-mode specific, rather than a generic criterion for all modes. As discussed in Section 2, each of the three possible failure modes, Figure 1-1, emphasizes different stress-strain condition, thus requiring different failure measures. For example, Mode I would be initiated by axial bending for which a CSED-based criterion or a strain-based criterion would be applicable as was used in the SAND90-2406 methodology [1]. However, the effects on failure strain of the oxide layer and the hydride rim were not considered in SAND90-2406. None of the available data in the CSED database involves axial bending. Mode II is a fracture mode that can extend by pure bending or by a combined bending and axial tension. In either case, the governing criterion is of the K_I/K_{IC} type. Mode III can extend from a pre-existing radial flaw, as was considered in SAND90-2406 [1], or evolve by a complex interaction mechanism involving circumferential and radial hydrides. This latter failure mechanism is the one believed to be the governing failure mechanism and is the subject of two EPRI recent studies, [2] and [3], as frequently mentioned in the present report.

Computation of SED in Finite Element Models

This concern addresses the need for guidance and consistency in computing SED results from finite element models and in using these results to make proper comparisons with CSED test data. There are two issues. The First concerns the choice of constitutive model used to represent the stress-strain behavior of the cladding from which SED is calculated. The Second concerns the element volume over which SED is computed in finite element models and how SED, so computed, relates to CSED calculated from test data.

Choice of Constitutive Model*: As mentioned in Item 1, CSED is a scalar quantity and is represented by a single number. It contains the integrated information of the stress strain curve from which it was derived. The integration process is not reversible, and given only CSED, there are an infinite number of stress-strain curves that can be constructed to yield the same CSED.*

Once the general characteristics and scale of the finite element model have been determined, the analyst makes a number of choices that include: (1) the types of elements to represent the various components and the interaction between components, (2) the size of elements and the mesh refinement necessary to achieve desired accuracy, and (3) the representation of material properties. To represent the elastic-plastic properties of high burnup fuel rod cladding, the analyst must decide on a constitutive material model to represent the material behavior of the cladding. Generally three properties are required to characterize behavior in order to calculate plastic strains. They are a yield function, a flow rule, and a hardening rule. Analysts will dotheir best to construct a reasonable material model. But of what? Zircalloy from a handbook? Moderately irradiated Zircalloy? High burnup Zircalloy from an axial tension test at low strain rate, 300 degrees C and 700 ppm hydrogen? Each of these material models will calculate SED, but they will all be quite different. Clearly analysts need guidance to develop an adequate material model to ensure a level of consistency among the numerous finite element models that may be constructed to address this issue on a case by case (?) basis.

Volume over which SED is computed*: In a typical finite element, stress and strain are computed at integration points within the element. From the integration points, stress and strain are extrapolated to the surface of the element, where they achieve their maximum values, or they can be integrated over the element volume to produce an average value of stress and strain over the element. Each of these three computations of stress and strain produces a different value of SED. And, if the element is located within a region of high* *stress gradient, each of these values can be significantly different and could easily be an order-of-magnitude apart. What then is the proper value of SED to report and use?*

The proper value is the one that best compares to the way in which CSED was computed from test data, which is directly linked to the questions raised in Item 2. For example, integration point estimates of SED may significantly over predict the probability(?) of cladding failure when compared to CSED data, if the CSED was computed over a smeared volume containing a tensile stress gradient. On-the-other-hand, CSED data computed at the location of maximum strain in a bending test would produce a severe under prediction of failure if compared to SED derived from the integration of stress and strain over the element, whereas for uniform axial tension this would be appropriate. Thus it must be clearly understood by the analyst how CSED data was computed so that SED can be properly calculated before comparisons are made.

Response to Computation of SED in Finite Element Models

Choice of Constitutive Model: The above comment deals with material constitutive modeling, which is the area of mechanics that is the engine that drives finite element codes. As such, it is the most important feature that distinguishes between a “good” analysis and a “bad” analysis, but the concerns raised in the comment are not made worse or better by applying the SED/CSED approach to failure evaluation. A full response to this comment is outside the scope of this report, but to respond specifically to the questions raised, an attempt will be made to address each point separately.

Zircaloy from a handbook is the starting point, to which one must add the effects of temperature, then the effects of irradiation, i.e., fast fluence, then high burnup effects, which would consist of oxide outer layer, hydride rim, hydride platelets of general orientation, then the effects of localized hydrides or lenses, and so on. By the time one gets through, the result is a highly heterogeneous, direction-dependent cladding that behaves not as a single material but as a multi-material structure. In standard material property tests, a single true-stress-true-strain curve for the cladding as a whole is produced as a function of the primary independent variables, namely, degree of cold work, fast fluence, temperature, and average hydrogen concentration. From such a stress-strain curve, one derives the elastic modulus, the yield strength, or more accurately the elastic limit, and the plastic modulus, all as function of the above-mentioned independent variables. An analytically skillful constitutive modeler then takes this information and generalizes it to a three-dimensional rate-dependent constitutive model in finite-strain formulation, again as function of the same variables. The finite element code applies this constitutive behavior to the element integration point volume, and, depending upon how many elements, type of element and number of integration points per element used, a response is calculated. The SED is calculated from that response at each integration point and then compared to the CSED. Both the CSED and SED are finite-volume properties, which means that the finer the finite element grid, the more accurate the calculations. However, the flip side of the accuracy question is how well does the stress-strain curve represent the behavior of the heterogeneous cladding, and can the material representation be improved by adding more elements through the cladding thickness? Another way of asking this question is, can we substitute spatial accuracy for material behavior accuracy? The answer is generally NO, unless the material constitutive model can specifically account for the spatial variation and behavioral regimes of material variables. For example, the use of more than one element through the thickness would improve the accuracy with regards to temperature dependence, but not with regards to the hydrogen concentration because it is not usually defined through the thickness.

In conclusion, most analysts are conscious of the need for spatial accuracy and they rely on experience to construct appropriately accurate finite element grids. However, accurate representation of material behavioral is a much more complicated problem, and has been the focus of much of the interaction with the Staff on high burnup issues [9].

Volume over which SED is computed: As discussed above, the SED is calculated at the element integration points because this is where the material response is calculated and where the input material properties, including the CSED, are used in the constitutive model. CSED is derived from mechanical properties tests and, as such, should not involve a stress gradient. By definition, the mechanical property stress-strain data, from which the material constitutive behavior is derived and the CSED is calculated, are a finite-volume property. Consequently, the calculated SED at any point in the cladding, which is also a finite-volume response, is consistent with the material property data. Now, because of finite element discretization, the input properties and the output response are averaged over a finite volume, namely the integration point volume, then the smaller the element the more accurate the representation for the input and the output variables. Irrespective of the finite element mesh size, the process of comparing the response SED to the input CSED is self-consistent. This is well understood by finite element analysts in choosing the appropriate finite element discretization to suit the problem.

Validation of Finite Element Predictions of Cladding Failure

One way to begin to address the concerns raised in Item 5 is to develop general finite element modeling guidelines based on direct comparisons of results from finite element models of test specimens to the results of the actual test. This approach is taken in Reference [5].

> *"The Expansion Due to Compression (EDC) test has been developed for the study of irradiated and hydrated cladding failure, under conditions of pellet cladding mechanical interaction, which are expected during a reactivity initiated accident (RIA). A finite element simulation of the EDC test is presented. The objective of the study is (i) to understand the deformation of the cladding during the experiment, including the effect of cladding material properties, and (ii) to provide information necessary for the development of failure criteria."*

Demonstrating that finite element models can reasonably duplicate the results of simple controlled static tests will develop confidence in the results from complex models used to predict fuel rod cladding failure during an accident event.

The CSED method presented by NEI brings a new approach to the problem of predicting cladding failure and is perhaps a better predictive tool than alternative empirical correlations such as critical strain level. As stated earlier, the concerns enumerated here, not only address the CSED method proposed by NEI, but also include concerns for the details of incorporating the CSED method into the entire methodology that will be used to predict fuel rod failure during a severe impact event. NEI and the NRC Staff will continue to work together to address these concerns. In this regard, it may be advisable to approach

ASTM Committee E8 to obtain views on the use of CSED for this application and to develop a standard test method the takes into account the significant variables.

Response to Validation of Finite Element Predictions of Cladding Failure

Motivation to validate non-linear finite element analyses against experimental results is an intrinsic part of the computational-mechanics culture, and is performed quite frequently. One seldom finds a Journal paper dealing with complex behavioral issues without finding experimental confirmation. In fact, in many instances, the only motivation for the paper is to show how well experimental results are predicted. The Staff comment is taken as a further emphasis to a well-established practice.

The EDC test referenced in the comment is a promising variation on material property testing. However, it suffers from a number of limitations that can be more complex to handle than other tests. First, the un-measurable variables, namely, the cladding hoop and axial stresses are inferred from the force-displacement measurements through modeling and analysis that requires a highly sophisticated constitutive model of the visco-plastic insert material. Second, the mechanical properties of the insert material are strongly time-temperature-dependent, which have to be determined first; and to determine these properties could become an end in itself. Third, the insert-cladding interaction in the axial direction puts the cladding in axial compression because of friction, which creates a tension-compression state of stress in the cladding instead of biaxial tension under PCMI. A tension-compression stress state produces a negative biaxiality effect on cladding deformations, i.e., it raises the total-elongation strain, and to a lesser degree the uniform-elongation strain, relative to the uniaxial case, whereas a reduction in these strain limits would be expected. Fourth, high temperature testing and strain-rate testing are virtually eliminated because of the visco-plastic behavior and the strong dependence on temperature of the insert material.

The suggestion to work with the NRC Staff on this general issue is a most welcome one, and we would welcome the opportunity to work together in a workshop setting to evaluate the feasibility of involving ASTM Committee 8 in this work.

Some Recent Test Information

A public meeting was held at Argonne National Laboratory on July 16-17, 2003, to review research in an NRC program that is being performed in cooperation with EPRI and other industry representatives. Highlights included a presentation on a high burnup fuel specimen that failed. It was stated that several medium-burnup and high-burnup cladding specimens in creep furnaces were brought down in temperature under full pressure to roughly simulate conditions of vacuum drying in a cask. The two medium-burnup specimens exhibited remarkable redistribution of their hydrides, with some now in the radial orientation, which can lead to cracking. The high-burnup specimen failed about half way through the cooldown and has not been examined yet. All initial temperatures were 400°C or less (ISG-11, Rev. 2 limits temperature to 400°C).

Although no additional information has been provided concerning this failure, staff offers that this test and data might provide some useful information for validating whether or not CSED would be an appropriate failure criterion for hydrided fuel cladding.

1. *"Analysis of High-Burnup Spent Fuel Subjected to Hypothetical Transportation Accidents, Part I and II,", presented by Joe Rashid and Albert Machiels, NRC-Industry Meeting, Washington, DC, October 23, 2002.*
2. *"A Cladding Failure Model for Fuel Rods Subjected to Operational and Accident Transients," Progress Report, J. Rashid, R. Montgomery and W. Lyon of ANATECH, and R. Yang of EPRI.*
3. *Hot Cell Examination of Extended Burnup Fuel from Calvert Cliffs-1. July 1994. EPRI TR-103302-V2.*
4. *"A Method for Determining the Spent Fuel Contribution to Transport Cask Containment Requirements," SANDIA Report, SAND90-2406, November 1992.*
5. *"Elastic-Plastic Deformation of a Nuclear Fuel Cladding Specimen under the Internal Pressure of a Polymer Pellet," Dufourneaud, et.al., Fifth World Congress on Computational Mechanics, July, 2002.*

Response to Some Recent Test Information

Failure of creep specimen C15 during cooldown under pressure to room temperature was totally unexpected. The radial hydrides concentration that was expected to develop in the middle section of the specimen was about 200 ppm. Applying the failure criteria in Figure 3-1 to this test indicates that a specimen with 200 ppm of radial hydrides if subjected to a tensile hoop stress would fail in the elastic regime at an SED value of 1-2 MPa. This is equivalent to a much higher stress level than the stress in the specimen. Thus, radial hydrides played no role in the failure of the specimen.

The failure occurred in the specimen's cold end weld region, presumably under the combined effects of high hydrogen concentration and high discontinuity stresses. Consequently, the failure of the C15 specimen cannot be used to validate the CSED criteria, as requested in the comment. However, a substitute test is chosen from a group of un-irradiated specimens that were pre-hydrided, then subjected to radial hydride treatments (RHT) at various stress levels in a three-piece tension-loading device. The specimens were subjected to ring compression tests to failure. The specimen with the highest RHT stress of 190 MPa, with an average hydrogen concentration in the range of 250-300 ppm, failed in the linear range of load displacement curve at a load level of 0.38 kN. The corresponding diametric displacement was 0.3 mm. Although the specimen was unirradiated, the radial hydride concentration, which would be similar to the C15 specimen, was sufficiently high to be the governing factor in the specimen failure. The precise value of radial hydride concentration was not known because of the uncertainty in the local stress level under which radial hydrides were created during the RHT; however, it is estimated to be in the range of 100-200 ppm.

A finite element simulation of the ring compression test for this specimen was performed, and the resulting force-displacement curve is shown in Figure 3-4. Except at the point of failure, the force-displacement curve does not depend on the radial hydrides to any significant degree to require precise knowledge of the concentration. The intent is to conduct the analysis up to the failure load level of 0.38 kN and then verify whether the calculated SED agrees with the CSED in Figure 3-1 for the estimated range of radial hydrides. The analysis revealed that the 0.38 kN compression load produced a hoop stress level of about 620 MPa at the inner surface of the cladding where failure initiation occurred. This stress level is in the elastic range of the material, for which the SED can be calculated from the simple formula, SED = $\sigma^2/(2E)$. Setting $\sigma = 620$ MPa and the elastic modulus $E = 85{,}000$ MPa, SED was calculated to be 2.26 MPa. This compares well with a CSED value from Figure 3-1 for radial hydride concentration in the middle range between 100-200 ppm. Although insufficient as a complete validation of the CSED methodology, this simple example is given in response to the request expressed in the review comment.

As a side comment, the above example also illustrates that radial hydrides do not act as pre-existing cracks even at room temperature. For radial hydride concentration in the estimated range, there is a high probability that a radial hydride platelet would be located at or close to the ID that it behaves as a crack initiator at a stress level equal to the yield strength of δ-hydride, which at room temperature is in the range of 180-200 MPa. This is less than one third of the stress calculated for the specimen at the failure load of 0.38 kN, which demonstrates that radial hydrides act as part of a composite rather than individually acting weak inclusions.

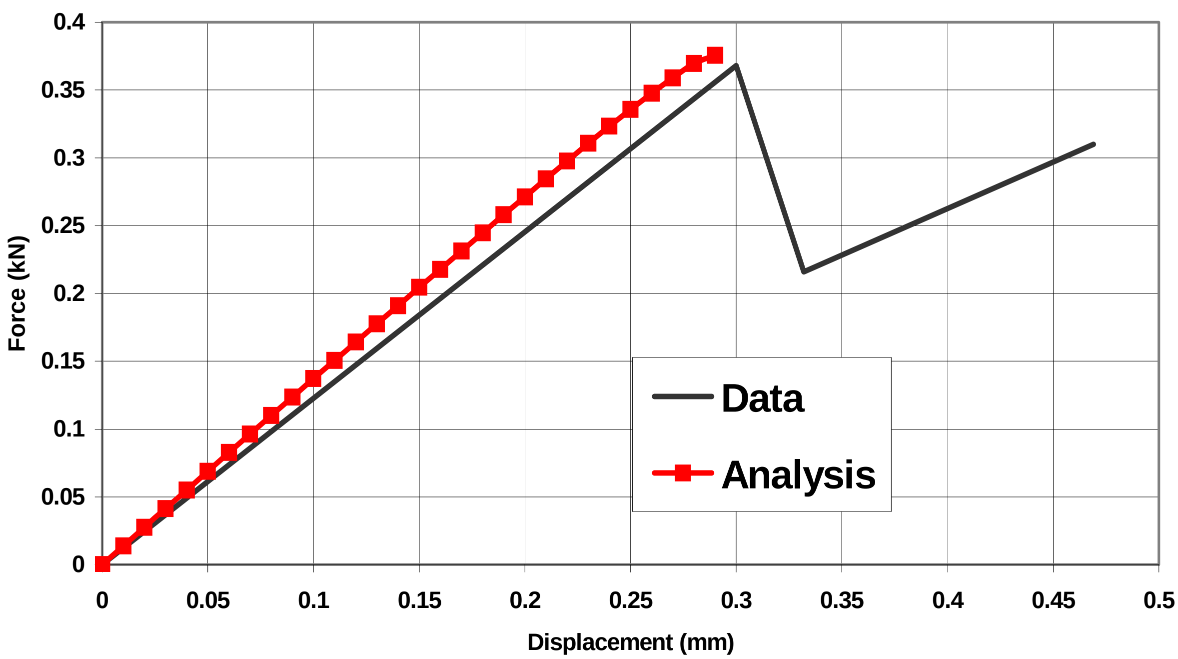


Figure 3-4
Force-Displacement Curve for ANL Ring Compression Test of an Unirradiated Specimen, Gas-Charged to 250-300 ppm in Hydrogen, Heat-Treated at 190 MPa from 400°C

4 CONCLUSIONS

The information provided in this report supports the original premise that the CSED best-fit correlation developed from stress-strain data represents a valid measure for the failure state of high-burnup cladding despite the experimental limitations of the database. Moreover, the CSED concept, unlike strain-based criteria, lends itself to the development of failure-mode-specific failure measures that recognize the effect of hydride morphology on cladding failure.

It is relevant to state that the present responses to the Staff comments draw on development work that post-dated the issuance of the review comments on August 15, 2003. That development work, which is described in the references cited in the text, would be a useful adjunct to this report.

5
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2. J. Y. R Rashid, M. M. Rashid, R. S. Dunham, *Development of Cladding Failure Criteria Using Damage-Based Metal/Hydride Mixture Model*. December 2004. EPRI Report 1009693.
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4. Letter from M. Wayne Hodges, Deputy Director, Office of Nuclear Material Safety and Safeguards to Steve Kraft, Director of Licensing, Nuclear Energy Institute, "Staff Comments on the Proposed Use of CSED for Prediction of Cladding Failure for High Burnup Fuel", August 15, 2003.
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8. R. O. Montgomery, N. Waeckel, R. Yang, *Topical Report on Reactivity Initiated Accident: Bases for RIA Fuel and Core Coolability Criteria. 2002*. EPRI Report 1002865.
9. A. J. Machiels, *Dry Storage of High-Burnup Spent Fuel – Responses to Nuclear Regulatory Commission Requests for Additional Information and Clarification*. November, 2003. EPRI Report 1009276.

A

APPENDIX A: TEXT OF “STAFF COMMENTS ON THE PROPOSED USE OF CSED FOR PREDICTION OF CLADDING FAILURE FOR HIGH BURNUP FUEL”

August 15, 2003

Steve Kraft
Director, Licensing
Nuclear Energy Institute
Suite 400
1776 I Street, N. W.
Washington, DC 20006-3708

SUBJECT: STAFF COMMENTS ON THE PROPOSED USE OF (CRITICAL STRAIN ENERGY DENSITY) CSED FOR PREDICTION OF CLADDING FAILURE FOR HIGH BURNUP FUEL

Dear Mr. Kraft

On October 23, 2002, staff from the Spent Fuel Project Office (SFPO) met with staff from the Nuclear Energy Institute (NEI), the Electric Power Research Institute (EPRI), Anatech, and industry representatives to discuss the proposed use of CSED as the measure most suitable for prediction of cladding failure for high burnup fuel rods. A representative from Anatech presented an overall approach to the CSED failure criterion.

During that meeting, SFPO staff stated that it would provide comments on the CSED failure criterion.

Based on the information presented at the meeting, the NRC Staff has a number of concerns with respect to the application of CSED to predicting fuel rod failure. These concerns are attached and focus on **six** broad areas that incorporate CSED into the methodology that will be used to predict fuel rod failure during a severe impact event.

We appreciate your effort to date on this difficult issue and look forward to continued interaction with you.

Sincerely

/RA/
M. Wayne Hodges, Deputy Director
Technical review Directorate
Spent Fuel Project Office
Office of Nuclear Material Safety
and Safeguards

Attachment: Assessment of the Application of CSED
to Predicting High Burnup fuel Rod Failues
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ML032310014

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Assessment of the Application of CSED
to
Predicting High Burnup Fuel Rod Failures

A most basic and useful description of a material's strength and toughness is its uniaxial stress-strain curve. The integrated area under a stress-strain curve is the total strain energy per unit volume (strain energy density, SED) absorbed by the material at failure. When SED is associated with failure criteria, it is commonly called the critical strain energy density (CSED).

At the joint NEI/NRC Meeting on 10/23/02, NEI proposed to use CSED as the measure most suitable for prediction of cladding failure for high burnup fuel rods. The NEI proposal (References 1 and 2) employs a set of assumptions and a first principles approach to develop equations that relate a CSED value for any material to its corresponding fracture toughness. Using a J-integral approach, an equation is developed that relates energy release rate to the SED in unflawed cladding. From this, it is concluded that failure of cladding with a longitudinal flaw can be predicted by calculating the SED of the cladding, without modeling the flaw or performing a fracture mechanics analysis, and comparing the calculated SED to the CSED determined from material tests.

Having established a relationship between CSED and fracture toughness parameters, the CSED data from ring tension tests, axial tension tests and burst tests are plotted against a cladding damage parameter (oxide thickness) in Figure 4 of Reference 2 (also included in Reference 1). A best estimate curve is drawn through the data. NEI proposes that this curve (or something similar) be used as the criteria to determine fuel rod failure by comparing CSED calculated from it, to SED values computed for fuel rods from finite element impact models.

Based on the information presented, the NRC Staff has a number of concerns with respect to the application of CSED to predicting fuel rod failure. These concerns are focused on six broad areas that incorporate CSED into the methodology that will be used to predict fuel rod failure during a severe impact event.

The six areas of concern are:

1. Development of a Stress-Strain behavior and Failure Database
2. Computation of CSED from the Stress-Strain / Failure Data
3. Accounting for Scatter in CSED Data
4. Accounting for Significant Physical Environmental Effects
5. Computation of SED in Finite Element Models
6. Validation of Finite Element Predictions of Cladding Failure

ATTACHMENT

Each of these concerns is discussed in further detail below.

1) Development of a Stress-Strain behavior and Failure Database

Stress-strain failure data must adequately reflect the effects of both the physical environment and damage environment to which the cladding is subjected during operation, transport and testing. The physical environment includes state of stress, temperature, strain-rate and test methods. The damage environment includes hydrogen content, hydride orientation, fast fluence and oxide thickness.

The Staff's specific concerns are focused on the effects on CSED of state of stress, strain rate, test methods and reporting of test data.

<u>State of Stress</u>: CSED is a scalar quantity and is not uniquely related to the state of stress from which it is derived. As such, there are an infinite number of states of stress that can produce the same value of CSED. Thus, axial tension, hoop tension, biaxial tension due to internal pressure, and other states of stress can each produce the same value of CSED. On-the-other-hand, because cracks, flaws and defects generally have an orientation associated with them, the failure of high burnup fuel cladding dependents directly on the state of stress in the cladding (i.e., an axially oriented crack is not susceptible to fracture from axial tension, but is from hoop tension).

Figure 4, presented by NEI, clearly shows that state of stress significantly affects the computed value of CSED. Therefore, separate curves that adequately account for the effect of state of stress on cladding failure are necessary.

<u>Strain Rate</u>: In general, as strain rate increases, material strength increases and ductility decreases. Since both strength and ductility are directly related to CSED, it would be expected that CSED would not be as greatly affected by strain rate, as rupture strain would be. Nevertheless, uncertainty exists, and as cladding becomes more and more brittle due to high burnup, the effects of strain rate on CSED may become more severe. Testing programs currently underway will hopefully be able to address this issue.

<u>Test Methods and Reporting of Test Data</u>: Procedures for conducting tests on fuel rod cladding and the methods for reducing and reporting data must be standardized and rigorous. Figure 4, presented by NEI, clearly shows that the CSED computed from ring tensile tests is significantly higher than the CSED computed from the axial tensile tests. Since irradiated Zircaloy cladding exhibits isotropic or near-isotropic behavior, as explained in Reference 2, one might conclude that the dramatic differences in calculated CSED are related to flaw orientation. But are they? Clearly the different values relate to the state of stress in the two types of specimens.

Figures 5-51 and 5-58 (and Figures 5-48 and 5-55) from Reference 3 provide a clue to explain these differences. Figure 5-51 shows a stress-strain curve of a tube (axial) tensile specimen from a high burnup fuel rod. Figure 5-58 shows a stress-strain curve of a ring tensile specimen taken from the same rod at a location only a couple of inches from the tube tensile specimen. Both the tube test and the ring test are uniaxial tension tests. What is observed from these figures is that the rupture strain in the ring test is almost an order of magnitude greater than in the tube test, and that the CSED is almost six times greater. This begs the question as to whether strain in the cladding material is actually what is being represented in the ring test stress-strain curve?

Round robin ring tensile testing performed by ANL, CEA, and the Russians have shown that ring tension test data are not a good measure of material strain capability, because measured strains from this test depend on test specimen size and the test apparatus, and are not a measure of cladding strain. The ring test tends to measure artificially high strains and, therefore, artificially high CSEDs, when these erroneous strains are used. This suggests that the currently proposed CSED correlation may seriously under predict the probability of failure. More importantly, it is obvious that greater care must be exercised in the methods used to extract and report test data.

There are additional concerns that place Figure 4 data in conflict with other sources. A methodology for determining fuel rod failure during a cask impact event was developed in Reference 4. The NEI presentation focused attention on modifying aspects of this methodology to accommodate high burnup fuel rod cladding. However, a discrepancy exists between information contained in Reference 4 and information presented by NEI. In Figure III-30 of Reference 4, rupture strain (which is directly proportional to CSED) is plotted against the cumulative probability of cladding failure for three different ratios of hoop stress to axial stress. This plot shows that as hoop stress increases, rupture strain (and CSED) dramatically decreases. This is exactly the opposite of the trend reflected in Figure 4, which shows CSED increasing as hoop stress increases. This discrepancy needs to be resolved.

2) Computation of CSED from the Stress-Strain / Failure Data

The concerns enumerated above lead directly to the concern for how CSED is calculated from test data.

- What is the precise methodology used to calculate CSED in the tube tensile test, the ring tensile test, the burst test and the bending test?
- What does the calculated CSED actually represent, and is it a true expression of the SED in the cladding alone?
- What is the volume over which CSED is calculated? Is it measured at a point, or, is it measured over a smeared volume by computing the total elongation over some gage length, as would be expected in the development of a typical stress-strain curve?

These questions are related to how the calculated CSED is actually used in failure prediction for cladding and is discussed again in Item 5 below.

3) Accounting for Scatter in CSED Data

There is significant scatter in the data presented by NEI correlating CSED with the cladding damage parameter (oxide thickness). The single, best-fit curve drawn through the data presented in Figure 4 does not adequately describe the failure probability of high burnup fuel cladding because of the large uncertainty in any estimate of CSED. Best estimate and lower bound (mean minus one standard deviation, or 84% non- exceedance probability) curves need to be constructed to quantify uncertainties in order that a probabilistic fuel rod failure evaluation can be performed, as intended in Reference 4.

[Comment: The CSED in Figure 4 appears to be based on total elongation strains at failure as opposed to uniform elongation strains. Use of uniform elongation strains from burst and axial tensile tests typically reduces the scatter in the CSED data by a factor of 3 to 4 as compared to use of total elongation strains from the burst and axial tensile tests. However, using uniform elongation strains will generally under predict CSED.]

4) Accounting for Significant Physical Environmental Effects

The failure mechanisms for fracture have sometimes proven to be too numerous and complex to trust the accuracy of empirical correlations, other than over narrow ranges of materials and test conditions prototypical of the intended application. For example, in the case of ferritic steels the existence of a transition temperature for ductile versus brittle fracture has complicated the efforts to develop correlations. In the case of zirconium alloys the complicating effects of temperature are further compounded by the effects of hydride orientation and total content on fracture toughness.

Indeed, further complexity is encountered as the state of damage in the cladding becomes more severe due to high burnup. In these circumstances it is important to develop empirical correlations within ranges of variables that more precisely reflect the physical conditions (state of stress, strain rate, etc.) to which the cladding is subjected. If cladding is subjected to axial tension at high strain rates, a correlation between CSED and the damage parameter for those specific physical conditions will produce far more meaningful results than a correlation between CSED and the damage parameter that includes all states of stress and low strain rates. Clear delineation among states of stress is necessary to develop better correlations with failure data and to minimize uncertainty.

5) Computation of SED in Finite Element Models

This concern addresses the need for guidance and consistency in computing SED results from finite element models and in using these results to make proper comparisons with CSED test data. There are two issues. The First concerns the choice of constuitive model used to represent the stress-strain behavior of the cladding from which SED is calculated. The Second concerns the element volume over which SED is computed in finite element models and how SED, so computed, relates to CSED calculated from test data.

Choice of Constitutive Model: As mentioned in Item 1, CSED is a scalar quantity and is represented by a single number. It contains the integrated information of the stress strain curve from which it was derived. The integration process is not reversible, and given only CSED, there are an infinite number of stress-strain curves that can be constructed to yield the same CSED.

Once the general characteristics and scale of the finite element model have been determined, the analyst makes a number of choices that include: (1) the types of elements to represent the various components and the interaction between components, (2) the size of elements and the mesh refinement necessary to achieve desired accuracy, and (3) the representation of material properties. To represent the elastic-plastic properties of high burnup fuel rod cladding, the analyst must decide on a constuitive material model to represent the material behavior of the cladding. Generally three properties are required to characterize behavior in order to calculate plastic strains. They are a yield function, a flow rule, and a hardening rule. Analysts will do their best to construct a reasonable material model. But of what? Zircalloy from a handbook? Moderately irradiated Zircalloy? High burnup Zircalloy from an axial tension test at low strain rate, 300 degrees C and 700 ppm hydrogen? Each of these material models will calculate SED, but they will all be quite different. Clearly analysts need guidance to develop an adequate material model to ensure a level of consistency among the numerous finite element models that may be constructed to address this issue on a cask by cask basis.

Volume over which SED is computed: In a typical finite element, stress and strain are computed at integration points within the element. From the integration points, stress and strain are extrapolated to the surface of the element, where they achieve their maximum values, or they can be integrated over the element volume to produce an average value of stress and strain over the element. Each of these three computations of stress and strain produces a different value of SED. And, if the element is located within a region of high stress gradient, each of these values can be significantly different and could easily be an order-of-magnitude apart. What then is the proper value of SED to report and use?

The proper value is the one that best compares to the way in which CSED was computed from test data, which is directly linked to the questions raised in Item 2. For example, integration point estimates of SED may significantly over predict the probably of cladding failure when compared to CSED data, if the CSED was computed over a smeared volume containing a tensile stress gradient. On-the-other-hand, CSED data computed at the location of maximum strain in a bending test would produce a severe under prediction of failure if compared to SED derived from the integration of stress and strain over the element, whereas for uniform axial tension this would be appropriate. Thus it must be clearly understood by the analyst how CSED data was computed so that SED can be properly calculated before comparisons are made.

6) Validation of Finite Element Predictions of Cladding Failure

One way to begin to address the concerns raised in Item 5 is to develop general finite element modeling guidelines based on direct comparisons of results from finite element models of test specimens to the results of the actual test. This approach is taken in Reference 5.

> “The Expansion Due to Compression (EDC) test has been developed for the study of irradiated and hydrated cladding failure, under conditions of pellet cladding mechanical interaction, which are expected during a reactivity initiated accident (RIA). A finite element simulation of the EDC test is presented. The objective of the study is (i) to understand the deformation of the cladding during the experiment, including the effect of cladding material properties, and (ii) to provide information necessary for the development of failure criteria.”

Demonstrating that finite element models can reasonably duplicate the results of simple controlled static tests will develop confidence in the results from complex models used to predict fuel rod cladding failure during an accident event.

The CSED method presented by NEI brings a new approach to the problem of predicting cladding failure and is perhaps a better predictive tool that alternative empirical correlations such as critical strain level. As stated earlier, the concerns enumerated here, not only address the CSED method proposed by NEI, but also include concerns for the details of incorporating the CSED method into the entire methodology that will be used to predict fuel rod failure during a severe impact event. NEI and the NRC Staff will continue to work together to address these concerns. In this regard, it may be advisable to approach ASTM Committee E8 to obtain views on the use of CSED for this application and to develop a standard test method the takes into account the significant variables.

Some Recent Test Information

A public meeting was held at Argonne National Laboratory on July 16-17, 2003, to review research in an NRC program that is being performed in cooperation with EPRI and other industry representatives. Highlights included a presentation on a high burnup fuel specimen that failed. It was stated that several medium-burnup and high-burnup cladding specimens in creep furnaces were brought down in temperature under full pressure to roughly simulate conditions of vacuum drying in a cask. The two medium- burnup specimens exhibited remarkable redistribution of their hydrides, with some now in the radial orientation, which can lead to cracking. The high-burnup specimen failed about half way through the cooldown and has not been examined yet. All initial temperatures were 400°C or less (ISG-11, Rev. 2 limits temperature to 400°C).

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2. "A Cladding Failure Model for Fuel Rods Subjected to Operational and Accident Transients," Progress Report, J. Rashid, R. Montgomery and W. Lyon of ANATECH, and R. Yang of EPRI.

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5. "Elastic-Plastic Deformation of a Nuclear Fuel Cladding Specimen under the Internal Pressure of a Polymer Pellet," Dufourneaud, et.al., Fifth World Congress on Computational Mechanics, July, 2002.

B

APPENDIX B: RESPONSE TO CATEGORY 1 COMMENTS, SEPTEMBER 2003

Response to NRC Staff Comments: "Assessment of the Application of CSED to Predicting High Burnup Fuel Rod Failures"

The following discussion is in response to NRC Staff comments issued September 1, 2003 on the CSED methodology. The Staff's comments identify the following areas of concern:

1. Development of a Stress-Strain behavior and Failure Database
2. Computation of CSED from the Stress-Strain/Failure Data
3. Accounting for Scatter in CSED Data
4. Accounting for Significant Physical Environmental Effects
5. Computation of SED in Finite Element Models
6. Validation of Finite Element Predictions of Cladding Failure

The response to the Staff comments is divided into two categories:

- Category-1 response addresses comments that are believed to be answerable without further analytical development. This response category is given here, but could require further communication with the Staff.
- Category-2 response addresses comments that are judged to require further work, including modeling and analysis, to construct a response to Staff's satisfaction.

Category-1 response is given for each point raised, identifying where Category-2 response would be needed. The original staff comments are shown in blue italics. First, we give a general introductory response.

GENERAL INTRODUCTORY RESPONSE

Under hypothetical accident conditions, there are three possible failure modes, as described in the SAND90-2406 methodology:

- Mode-I: Bending-induced transverse tearing at the outer cladding surface.
- Mode-II: Extension of Mode-I as a breakage mode under bending.
- Mode-III: A longitudinal crack at the inner (or outer) surface caused by rod-to-rod impact in a horizontally oriented cask drop.

These modes are depicted in Figure 1, with 3D configuration shown in Figure 2. Failure Mode-III was shown in SAND90-2406 to have the highest failure probability. The failure criteria used is a strain limit ε_f for Mode-I, and fracture toughness K_{IC} for Modes II and III. The cladding burnup level was not in the high-burnup range, and, there was no consideration of the effects of hydrogen or hydride orientation on the mechanical properties or failure configuration. The question at hand is: how do the failure criteria and failure modes change for high burnup fuel?

Failure modes remain the same, with the exception that two pairs of Mode-III cracks could occur simultaneously in the same cross section, namely, 2 ID cracks and 2 OD cracks at orthogonal diameters. Such a potential failure configuration would be postulated as a worst-case consequence of radial hydrides. Radial hydrides have little or no effect on Mode-I and Mode-II. It would seem then that there is no reason to deviate from the SAND90-2406 methodology, but new failure limits will have to be developed. In order to draw a comparison between low-burnup and high-burnup cladding failure scenarios under hypothetical drop events, it is useful to review how failure sequences were treated in the SAND90-2406 methodology.

It was straightforward to define the failure sequence and the applicable failure criteria for Modes-I and -II failures. For example, initiation of Mode-I based on local ductility ε_f was a logical choice. It was then assumed that, once initiated, the failure would instantaneously progress to a depth equal to the cladding thickness and become a Mode-II failure. At this stage, knowing the crack depth, it became possible to calculate a K_I value to compare to the K_{IC} from data for irradiated material, which was available mainly for Zircaloy-2 pressure tubes. Once the condition that $K_I \geq K_{IC}$ was reached, it was assumed that the crack would extend instantaneously to become a guillotine break. This assumption was, of course, conservative because in pure bending the crack is extending towards the compression area of the cross section.

With regards to Mode-III, the process was also straightforward. For this case, it was assumed that a Mode-III failure would initiate from a PCI crack, and an initial crack depth needed to be defined. For that, a side analysis, using the EPRI fuel behavior code FREY, which is the predecessor to FALCON, was carried out to determine the maximum possible PCI crack depth before it became a through-wall crack during in-reactor service. Using those results, it became possible to calculate, again from side analysis, a K_I value to compare to the same K_{IC} used for Mode-II. In order to maximize the K_I value, it was assumed that the PCI crack was directly under the point of contact. However, K_I showed very rapid attenuation with the circumferential angle, within a few degrees, which provided a basis for a probabilistic distribution for crack spacing.

We will now examine the spent fuel condition at the end of the dry storage period to assess how the approach described above may change for high burnup cladding?

Previously submitted documents to NRC, References [I-V], presented various types of analyses for cladding with the following limiting conditions assumed to exist:

(1) Creep strains of a few percent
(2) Circumferential hydrides of some 600 ppm
(3) Radial hydrides less than 100 ppm
(4) Oxide thickness of 100 microns or higher
(5) A hydride rim of about 50 microns
(6) Hydride lenses up to 50% deep
(7) Fast fluence of 1.2 E25 n/m^2

Except for considering the effects of fast fluence on mechanical properties, none of the above conditions were considered in the SAND90-2406 methodology, and some were not even considered to exist. As mentioned earlier, the potential failure modes remain virtually the same. However, the criteria will be different, and the analysis method will be more complicated. What are the similarities and the differences that have to be considered? The following comments serve as a starting point:

(1) The criterion for Mode-I failure initiation, namely ε_f, could remain the same, but with values different from those used in SAND90-2406. Moreover, the presence of the oxide and hydride rim will have to be accounted for, both in the analysis and the failure criterion. Similarly, the creep strain becomes a pre-existing condition.

(2) For the same reasons stated in (1) above, K_{IC} will have to be re-quantified to account for the higher fluence and the presence of circumferential hydrides. Unfortunately, fracture data to permit such development is meager, and other means will be needed to extend Fracture Mechanics methods to high-burnup cladding. Further, the fracture mechanics analysis to determine the initial crack depth for Mode-II will not be as straightforward because of the multi-material structure of the cladding, namely, oxide, hydride rim and possibly hydride lens.

(3) Other than the need for updating the failure criteria for Modes I and II, and the added complexity in the analysis approach, failure evaluation involving these two modes will not be significantly different from the SAND90-2406 methodology. However, experimental evidence indicates that spacer grids in high-burnup assemblies exhibit more compliant behavior, i.e., reduced stiffness, during lateral compression tests. This means that fuel rod bending at spacer grids will be minimal, and the bending deformations at the end tie-plates in the non-fueled sections of the rods will remain small contributor to the failure probability. This behavior further reduces the effects of Modes I and II, relative to Mode III, and may in fact remove them from consideration altogether.

(4) The greatest effect of high-burnup service is for Mode-III failure. In the first place, a different K_{IC} from that used for Mode-II will be required because of the cladding structure is no longer dependent on the homogeneous effect of fast fluence alone, and will involve the presence of circumferential and radial hydrides. It is relevant to mention, parenthetically, that the concentration of radial hydrides will vary statistically over a wide range within the cask, from nearly zero for rods with lower fission-gas pressure and temperature to a few tens of ppm at the upper range of temperature and pressure. Fracture tests to derive a K_{IC} for cladding with any amount of radial hydrides are non-existent, and the issue is only now attracting the attention of testing laboratories. Secondly, pre-existing PCI cracks and their circumferential distribution may not be the governing initial conditions for Mode-III failure analysis. Consequently, it will be difficult, if not impossible, to define the initial conditions for fracture mechanics calculations. For example, should we consider radial hydrides as initial cracks, and further how do we define spacing and distribution of such hypothetical crack? The answer to this question is NO, because available data indicates that radial hydrides do not behave like pre-existing cracks even at concentrations significantly higher than can be expected at the end of the dry storage period. This will be demonstrated in a **Category-2 response - 1** – also see Point 5 below.

(5) Consequent to the above comment, and because the cladding structure consists of both circumferential and radial hydrides with highly complex interaction between them, we envisage that Mode-III failure analysis will rely very heavily on the CSED methodology. Testing laboratories have begun to generate CSED data for un-irradiated and irradiated cladding with radial hydrides and mixed radial and circumferential hydrides. This should be contrasted with the nearly non-existent state of the art of LEFM/K_{IC} data for similar cladding conditions. Further information will be provided as a **Category-2 response - 2**. This will involve the use of two recent developments, namely, the Hydride Precipitation Model and the Three-Phase Mixture Model, to derive a Mode-III failure criterion for application to spent fuel transportation.

(6) It can be concluded from the above comments that the adaptation of the SAND90-2406 methodology to high burnup fuel (already accepted by the NRC for application to hypothetical transportation accidents), will have to involve new concepts in failure analysis. In view of the fact that failure Modes I and II are shown to have even lower effect relative to Mode III than in the low-burnup case, the failure evaluation will mainly be concerned with Mode III. Further justification for relying on Mode-III evaluation will be provided as a **Category-2 response - 3**.

SPECIFIC RESPONSE TO STAFF COMMENTS

1) Development of a Stress-Strain behavior and Failure Database

Stress-strain failure data must adequately reflect the effects of both the physical environment and damage environment to which the cladding is subjected during operation, transport and testing. The physical environment includes state of stress, temperature, strain-rate and test methods. The damage environment includes hydrogen content, hydride orientation, fast fluence and oxide thickness.

The Staff's specific concerns are focused on the effects on CSED of state of stress, strain rate, test methods and reporting of test data.

1.1 State of Stress: CSED is a scalar quantity and is not uniquely related to the state of stress from which it is derived. As such, there are an infinite number of states of stress that can produce the same value of CSED. Thus, axial tension, hoop tension, biaxial tension due to internal pressure, and other states of stress can each produce the same value of CSED. On-the-other-hand, because cracks, flaws and defects generally have an orientation associated with them, the failure of high burnup fuel cladding dependents directly on the state of stress in the cladding (i.e., an axially oriented crack is not susceptible to fracture from axial tension, but is from hoop tension).

Figure 4, presented by NEI, clearly shows that state of stress significantly affects the computed value of CSED. Therefore, separate curves that adequately account for the effect of state of stress on cladding failure are necessary.

1.1 Response

First, we must distinguish between CSED and SED. CSED is the criterion, and is derived from material property tests. SED, on the other hand is calculated in the analysis code in response to the loading to which the structure is subjected. Both are energy quantities: CSED represents the capacity and SED represents the demand.

It is true that different states of stress can produce the same CSED, *"CSED is a scalar quantity and is not uniquely related to the state of stress from which it is derived. As such, there are an infinite number of states of stress that can produce the same value of CSED. Thus, axial tension, hoop tension, biaxial tension due to internal pressure, and other states of stress can each produce the same value of CSED",* but it is also true that each of these states of stress represents a failure state. Each data point in the CSED data says exactly that. However, the CSED curve is the best fit of all data points that came from separate tests, namely, axial tension, ring tension and pressurized tubes. In that sense, the CSED curve is more accurately characterized as an approximation rather than non-unique. Considering the current application of the CSED methodology, which is in reactivity-initiated accidents (RIA), it is appropriate to use the best-fit curve. This is because the cladding experiences three-dimensional states of stress induced by the pellet-cladding mechanical interaction (PCMI) loading, namely, small radial compression, large hoop tension and equally large axial tension. These loading conditions are individually simulated by the mechanical properties tests used to quantify CSED, namely, axial tension, ring tension and closed-tube burst tests.

Now, can the same argument be made for cladding under hypothetical transportation accidents? The answer is "yes" for a failure mode of type Mode-III depicted in Figure 1. Under Mode-III failure in a spent fuel rod, the cladding cross section sees a complex three-dimensional state of stress-strain due to internal gas pressure, radial and axial PCMI and a knife-edge longitudinal impact load. Applying separate direction-specific CSED curves as suggested by the comment, "*Therefore, separate curves that adequately account for the effect of state of stress on cladding failure are necessary*" is not only intractable, but also would involve much greater uncertainty than applying a single best-fit curve that represent contributions from all stress/strain components. However, the suggestion is valid for Mode-I failure where the primary loading is axial bending. As a corollary to this, the axial CSED data could be used to derive a fracture toughness value for use in Mode-II failure from the K_{IC}-CSED correlation.

1.2 Strain Rate: In general, as strain rate increases, material strength increases and ductility decreases. Since both strength and ductility are directly related to CSED, it would be expected that CSED would not be as greatly affected by strain rate, as rupture strain would be. Nevertheless, uncertainty exists, and as cladding becomes more and more brittle due to high burnup, the effects of strain rate on CSED may become more severe. Testing programs currently underway will hopefully be able to address this issue.

1.2 Response

As the comment indicates, CSED is not very sensitive to the strain rate for the loading rates anticipated. However, the comment seems to imply that separate consideration should be given to the rupture strain. Since the effect of strain rate on rupture strain is already accounted for in the CSED, we do not see further need to consider additional uncertainty.

Material property data are usually generated for strain rates of the order of 10^{-3} per second. However, significant differences in material response begin to show up around 10^1 - 10^2/s. Experience with cask drop tests on concrete targets show that the cask's impulse momentum reaches its maximum value in about 3-4 milliseconds. Fuel rod's maximum response would be delayed by at least several milliseconds, which brings the time it takes the cladding strain to reach 1%, say, about 10 milliseconds, indicating a strain rate of about 1/s. This is a factor of 10 smaller than the range of high strain rate effects. In accident analysis, however, the larger concern is not the strain-rate effect on material behavior but rather the effect of the dynamic amplification of the loading. This brings in the question of whether realistic targets versus unyielding target should be considered in the analysis. This question will be addressed at the appropriate time.

<u>1.3 Test Methods and Reporting of Test Data</u>: Procedures for conducting tests on fuel rod cladding and the methods for reducing and reporting data must be standardized and rigorous. Figure 4, presented by NEI, clearly shows that the CSED computed from ring tensile tests is significantly higher than the CSED computed from the axial tensile tests. Since irradiated Zircaloy cladding exhibits isotropic or near-isotropic behavior, as explained in Reference 2, one might conclude that the dramatic differences in calculated CSED are related to flaw orientation. But are they? Clearly the different values relate to the state of stress in the two types of specimens.

Figures 5-51 and 5-58 (and Figures 5-48 and 5-55) from Reference 3 provide a clue to explain these differences. Figure 5-51 shows a stress-strain curve of a tube (axial) tensile specimen from a high burnup fuel rod. Figure 5-58 shows a stress-strain curve of a ring tensile specimen taken from the same rod at a location only a couple of inches from the tube tensile specimen. Both the tube test and the ring test are uniaxial tension tests. What is observed from these figures is that the rupture strain in the ring test is almost an order of magnitude greater than in the tube test, and that the CSED is almost six times greater. This begs the question as to whether strain in the cladding material is actually what is being represented in the ring test stress-strain curve?

Round robin ring tensile testing performed by ANL, CEA, and the Russians have shown that ring tension test data are not a good measure of material strain capability, because measured strains from this test depend on test specimen size and the test apparatus, and are not a measure of cladding strain. The ring test tends to measure artificially high strains and, therefore, artificially high CSEDs, when these erroneous strains are used. This suggests that the currently proposed CSED correlation may seriously under predict the probability of failure. More importantly, it is obvious that greater care must be exercised in the methods used to extract and report test data.

There are additional concerns that place Figure 4 data in conflict with other sources. A methodology for determining fuel rod failure during a cask impact event was developed in Reference 4. The NEI presentation focused attention on modifying aspects of this methodology to accommodate high burnup fuel rod cladding. However, a discrepancy exists between information contained in Reference 4 and information presented by NEI. In Figure III-30 of Reference 4, rupture strain (which is directly proportional to CSED) is plotted against the cumulative probability of cladding failure for three different ratios of hoop stress to axial stress. This plot shows that as hoop stress increases, rupture strain (and CSED) dramatically decreases. This is exactly the opposite of the trend reflected in Figure 4, which shows CSED increasing as hoop stress increases. This discrepancy needs to be resolved.

1.3 Response

The points raised here deal with the well-known vexing issues associated with material properties testing of highly irradiated cladding. The difficulties stem from the fact that the analyst who is tasked with explaining fuel rod behavior must use data generated by somebody else and perhaps for different purposes. Testing laboratories have now adopted the CSED concept and are beginning to produce data in the form of CSED directly. The data referred to by the reviewer is the first set of data that was ever utilized in generating the CSED curve. The process of constructing the CSED, as described in cited Reference 2, used the information reported by the testing laboratory, which consisted of yield strength, ultimate strength, uniform elongation and total elongation. Adding elastic modulus from the literature, a piecewise continuous stress-strain curve was constructed, which was then integrated to obtain the CSED. Adjustment factors were applied to the uniaxial data to make the whole data set applicable to the multi-axial stress states experienced by the cladding in an RIA transient. The best-fit curve developed for the data proved to be more applicable to the interpretation of RIA tests, which were accurately predicted, than any individual data point or group of points in the data base. The whole process was based on judgment and intuitive interpretation of data, and sometimes brute-force was substituted for the lack of information.

Since then, however, more data became available, and much of this data was generated with CSED in mind. As noted in the introductory response, application of the CSED methodology to the analysis of hypothetical transportation accidents will not be by direct transfer of the present CSED methodology, but rather by meticulous adaptation of the methodology to the specific requirement of the failure modes expected. This will involve the reprocessing of the present database, adding additional data from available sources and, most importantly, the application of newly developed models to the analysis of material behavior. These include the Hydride Precipitation Model and the Three-Phase Mixture Model, which have been submitted to NRC as part of industry response to previously issued RAIs. This work will require significant effort, and will be provided as a **Category-2 response – 2**, as already stated in Point 5 of the introductory response**.** When this work is completed, many of the points raised in the above set of comments, and particularly the reference to Reference 4 in the last paragraph, will be addressed.

2) Computation of CSED from the Stress-Strain / Failure Data

The concerns enumerated above lead directly to the concern for how CSED is calculated from test data.

- *What is the precise methodology used to calculate CSED in the tube tensile test, the ring tensile test, the burst test and the bending test?*
- *What does the calculated CSED actually represent, and is it a true expression of the SED in the cladding alone?*

- *What is the volume over which CSED is calculated? Is it measured at a point, or, is it measured over a smeared volume by computing the total elongation over some gage length, as would be expected in the development of a typical stress-strain curve?*

These questions are related to how the calculated CSED is actually used in failure prediction for cladding and is discussed again in Item 5 below.

2. Response

The methodology of calculating the CSED is given in the response to the previous comment, and also in Reference 2 cited by the reviewer. Incidentally, there are no bending tests in the CSED database.

The CSED is the Critical Strain Energy Density, which represents the maximum energy per unit volume the material can resist. SED, is the Strain Energy Density delivered to the component during the loading event, e.g. control rod ejection, and is computed by the analysis code as function of spatial position. Failure of the cladding is indicated when the maximum value of SED at any spatial position in the cladding reaches a value equal to CSED.

Since the CSED is the area under the stress-strain curve, it is the property of a material point and has the units of $J/m^3 = N\text{-}m/m^3 = N/m^2 = Pa$. In British units, it is $lb\text{-}in/in^3 = lb/in^2 = psi$.

3) Accounting for Scatter in CSED Data

There is significant scatter in the data presented by NEI correlating CSED with the cladding damage parameter (oxide thickness). The single, best-fit curve drawn through the data presented in Figure 4 does not adequately describe the failure probability of high burnup fuel cladding because of the large uncertainty in any estimate of CSED. Best estimate and lower bound (mean minus one standard deviation, or 84% non-exceedance probability) curves need to be constructed to quantify uncertainties in order that a probabilistic fuel rod failure evaluation can be performed, as intended in Reference 4.

[Comment: The CSED in Figure 4 appears to be based on total elongation strains at failure as opposed to uniform elongation strains. Use of uniform elongation strains from burst and axial tensile tests typically reduces the scatter in the CSED data by a factor of 3 to 4 as compared to use of total elongation strains from the burst and axial tensile tests. However, using uniform elongation strains will generally under predict CSED.]

3. Response

The scatter in the data is unavoidable. A statistical analysis of the CSED data that will be used in transportation accident analysis will, in any case, be a part of probabilistic failure evaluation. However, if this information is required earlier, it can be provided as a **Category-2 response - 4.**

4) Accounting for Significant Physical Environmental Effects

The failure mechanisms for fracture have sometimes proven to be too numerous and complex to trust the accuracy of empirical correlations, other than over narrow ranges of materials and test conditions prototypical of the intended application. For example, in the case of ferritic steels the existence of a transition temperature for ductile versus brittle fracture has complicated the efforts to develop correlations. In the case of zirconium alloys the complicating effects of temperature are further compounded by the effects of hydride orientation and total content on fracture toughness.

Indeed, further complexity is encountered as the state of damage in the cladding becomes more severe due to high burnup. In these circumstances it is important to develop empirical correlations within ranges of variables that more precisely reflect the physical conditions (state of stress, strain rate, etc.) to which the cladding is subjected. If cladding is subjected to axial tension at high strain rates, a correlation between CSED and the damage parameter for those specific physical conditions will produce far more meaningful results than a correlation between CSED and the damage parameter that includes all states of stress and low strain rates. Clear delineation among states of stress is necessary to develop better correlations with failure data and to minimize uncertainty.

4. Response

The points raised in the above comment are valid in general and are especially relevant for the cladding loading that could exist during transportation accidents. However, before discussing transportation- specific issues, it is important to mention that the current application of the CSED is compatible with the cautionary message expressed in the above comment.

The CSED criterion was developed for application to RIA, for which the failure mode is an axial crack under PCMI loading. As previously mentioned, this type of loading, which is driven by the radial and axial expansion of very hot fuel pellets against relatively cold cladding, produces stresses and strains in the hoop and axial directions that are of almost equal intensity. The data used in developing the CSED curve involve similar stress-strain states, and therefore were appropriately utilized.

With regards to transportation accident analysis, what we plan to do is to develop a failure measure (criterion) that is appropriate for a particular failure mode, rather than a generic criterion for all modes. As discussed in the introductory response, each of the three possible failure modes, Figure 1, emphasize different stress-strain condition, thus requiring different failure measures. For example, Mode I would be initiated by axial bending for which a strain criterion would be appropriate as was used in the SAND90-2406 methodology. However, the effects of the oxide layer and the hydride rim were not considered. None of the available data in the CSED database involves axial bending, and current bending tests at ANL and elsewhere are concerned with post-quench conditions for LOCA. Similarly, Mode-II is a fracture mode that can extend by pure bending or by a combined bending and axial loading. In either case, the governing criterion is of the K_I/K_{IC} type. Finally, we have to deal with Mode III, which is the most important failure mode and also the least tractable. This failure mode can extend from a pre-existing radial flaw, which is the simplest and most tractable but not necessarily the most governing failure scenario, or evolve by a complex interaction mechanism involving circumferential and radial hydrides, which is likely to be the relevant failure mechanism. The stress-strain state in the cross section during a drop event would involve a highly localized hoop stress-strain state superimposed on a general state of hoop stress due to gas pressure and hoop strain due to creep. For this mode, we envisage the failure criterion to be based on a CSED type correlation. New data is now beginning to be developed for irradiated and un-irradiated cladding with radial and circumferential hydrides, which will make the development of a CSED correlation tractable.

5) Computation of SED in Finite Element Models

This concern addresses the need for guidance and consistency in computing SED results from finite element models and in using these results to make proper comparisons with CSED test data. There are two issues. The First concerns the choice of constitutive model used to represent the stress-strain behavior of the cladding from which SED is calculated. The Second concerns the element volume over which SED is computed in finite element models and how SED, so computed, relates to CSED calculated from test data.

<u>5.1 Choice of Constitutive Model</u>: As mentioned in Item 1, CSED is a scalar quantity and is represented by a single number. It contains the integrated information of the stress strain curve from which it was derived. The integration process is not reversible, and given only CSED, there are an infinite number of stress-strain curves that can be constructed to yield the same CSED.

Once the general characteristics and scale of the finite element model have been determined, the analyst makes a number of choices that include: (1) the types of elements to represent the various components and the interaction between components, (2) the size of elements and the mesh refinement necessary to achieve desired accuracy, and (3) the representation of material properties. To represent the elastic-plastic properties of high burnup fuel rod cladding, the analyst must decide on a constitutive material model to represent the material behavior of the cladding. Generally three properties are required to characterize behavior in order to calculate *plastic strains. They are a yield function, a flow rule, and a hardening rule. Analysts will do their best to construct a reasonable material model. But of what? Zircalloy from a handbook? Moderately irradiated Zircalloy? High burnup Zircalloy from an axial tension test at low strain rate, 300 degrees C and 700 ppm hydrogen? Each of these material models will calculate SED, but they will all be quite different. Clearly analysts need guidance to develop an adequate material model to ensure a level of consistency among the numerous finite element models that may be constructed to address this issue on a case by case (?) basis.*

5.1 Response

The above comment deals with material constitutive modeling, which is the area of mechanics that is the engine that drives finite element codes. As such, it is the feature that distinguishes between a "good" analysis and a "bad" analysis, but the concerns raised by the reviewer are not made worse or better by applying the SED/CSED approach to failure evaluation. To give this comment justice, would require more space than perhaps was intended by the reviewer, but to respond specifically to the questions raised we will attempt to address each point separately.

Zircaloy from a handbook is the starting point, to which one must add the effects of *temperature*, then the effects of *irradiation*, i.e., fast fluence, then *high burnup* effects, which would consist of *oxide* outer layer, *hydride rim, hydride platelets* of general orientation, then the effects of *localized hydrides* or lenses, and so on. By the time one gets through, he will have a highly heterogeneous direction-dependent cladding that behaves not as a single material but as a multi-material structure. In standard material property tests, a single true-stress-true-strain curve for the cladding as a whole is produced as a function of the primary independent variables, namely, degree of cold work, fast fluence, temperature, and hydrogen concentration. From such a stress-strain curve one derives the elastic modulus, the yield strength, or more accurately the elastic limit, and the plastic modulus, all as function of the above-mentioned independent variables. An analytically skillful constitutive modeler then takes this information and generalizes it to a three-dimensional rate-dependent constitutive model in finite-strain formulation, again as function of the same variables. The finite element code applies this constitutive behavior to the element integration point volume, and, depending upon how many elements, type of element and number of integration points per element used, an accurate response is said to be calculated. The SED is calculated from that response at each integration point and then compared to the CSED. This implies that the CSED is defined as a point property, but the computed response, i.e., the SED, is a finite-volume property, which means that the finer the finite element grid the more accurate the calculations. However, the flip side of the accuracy question is how well does the stress-strain curve represent the behavior of the heterogeneous cladding, and can the material representation be improved by adding more elements through the cladding? Another way of asking this question is, can we substitute spatial accuracy for material-behavior accuracy? The answer is generally NO, unless the material constitutive model can specifically account for the spatial variation and behavioral regimes of material variables. For example, the use of more than one element through the thickness would improve the accuracy with regards to temperature dependence, but not with regards to the hydrogen concentration because it is not usually defined through the thickness.

In conclusion, most analysts are conscious of the need for spatial accuracy and they rely on experience to construct appropriately accurate finite element grids. However, accurate representation of material behavioral is a much more complicated problem, and has been the focus of much of the interaction with the Staff on high burnup issues. A case in point is the Hydride Precipitation Model and the Three-Phase Mixture Model cited earlier.

5.2 Volume over which SED is computed: In a typical finite element, stress and strain are computed at integration points within the element. From the integration points, stress and strain are extrapolated to the surface of the element, where they achieve their maximum values, or they can be integrated over the element volume to produce an average value of stress and strain over the element. Each of these three computations of stress and strain produces a different value of SED. And, if the element is located within a region of high stress gradient, each of these values can be significantly different and could easily be an order-of-magnitude apart. What then is the proper value of SED to report and use?

The proper value is the one that best compares to the way in which CSED was computed from test data, which is directly linked to the questions raised in Item 2. For example, integration point estimates of SED may significantly over predict the probability of cladding failure when compared to CSED data, if the CSED was computed over a smeared volume containing a tensile stress gradient. On-the-other-hand, CSED data computed at the location of maximum strain in a bending test would produce a severe under prediction of failure if compared to SED derived from the integration of stress and strain over the element, whereas for uniform axial tension this would be appropriate. Thus it must be clearly understood by the analyst how CSED data was computed so that SED can be properly calculated before comparisons are made.

5.2 Response

As discussed in 5.1 Response, the SED is calculated at the element integration points because this is where the material response is calculated and where the input material properties, including the CSED, are used in the constitutive model. CSED is derived from mechanical properties tests and, as such, does not involve a stress gradient. Now, because of the finite element discretization, the input properties and the output response is smeared over a finite volume, namely the integration point volume, then the process of comparing the response SED to the input CSED is self consistent. This is well understood by finite element analysts.

6) Validation of Finite Element Predictions of Cladding Failure

6.1. One way to begin to address the concerns raised in Item 5 is to develop general finite element modeling guidelines based on direct comparisons of results from finite element models of test specimens to the results of the actual test. This approach is taken in Reference 5.

> *"The Expansion Due to Compression (EDC) test has been developed for the study of irradiated and hydrated cladding failure, under conditions of pellet cladding mechanical interaction, which are expected during a reactivity initiated accident (RIA). A finite element simulation of the EDC test is presented. The objective of the study is (i) to understand the deformation of the cladding during the experiment, including the effect of cladding material properties, and (ii) to provide information necessary for the development of failure criteria."*

Demonstrating that finite element models can reasonably duplicate the results of simple controlled static tests will develop confidence in the results from complex models used to predict fuel rod cladding failure during an accident event.

6.1 Response

Motivation to validate non-linear finite element analyses against experimental results is an intrinsic part of the computational-mechanics culture, and is performed quite frequently. One seldom finds a Journal paper dealing with complex behavioral issues without finding experimental confirmation. In fact, in many instances the only motivation for the paper is to show how well experimental results are predicted. As a further emphasis to a well-established practice, the reviewer's comment is well taken.

6.2. The CSED method presented by NEI brings a new approach to the problem of predicting cladding failure and is perhaps a better predictive tool than alternative empirical correlations such as critical strain level. As stated earlier, the concerns enumerated here, not only address the CSED method proposed by NEI, but also include concerns for the details of incorporating the CSED method into the entire methodology that will be used to predict fuel rod failure during a severe impact event. NEI and the NRC Staff will continue to work together to address these concerns. In this regard, it may be advisable to approach ASTM Committee E8 to obtain views on the use of CSED for this application and to develop a standard test method the takes into account the significant variables.

6.2 Response

This is an interesting suggestion, and we welcome the opportunity to work together in a workshop setting to evaluate feasibility and generate some ideas.

7. Some Recent Test Information

A public meeting was held at Argonne National Laboratory on July 16-17, 2003, to review research in an NRC program that is being performed in cooperation with EPRI and other industry representatives. Highlights included a presentation on a high burnup fuel specimen that failed. It was stated that several medium-burnup and high-burnup cladding specimens in creep furnaces were brought down in temperature under full pressure to roughly simulate conditions of vacuum drying in a cask. The two medium-burnup specimens exhibited remarkable redistribution of their hydrides, with some now in the radial orientation, which can lead to cracking. The high-burnup specimen failed about half way through the cooldown and has not been examined yet. All initial temperatures were 400^{o}C or less (ISG-11, Rev. 2 limits temperature to 400^{o}C).

Although no additional information has been provided concerning this failure, staff offers that this test and data might provide some useful information for validating whether or not CSED would be an appropriate failure criterion for hydrided fuel cladding.

7. Response

After more information becomes available for this test specimen, an analysis will be performed to evaluate the magnitude of radial hydrides and the extent to which the radial hydrides could have been responsible for the failure. The results of this analysis will be provided as a **Category-2 response - 5**.

8. List of Category-2 Response Items

(1) Analysis of failure data for specimens with radial hydrides and mixed, circumferential and radial, hydrides.

(2) Updating the CSED database and the development of CSED-based failure criteria for Mode-III.

(3) Mode-III failure as the governing failure mode for hypothetical transportation accidents.

(4) Statistical analysis of the CSED current database and new data that may be added.

(5) Analysis of ANL's C15 specimen.

9. References – List of Documents Previously Provided to NRC

I. Creep as the Limiting Mechanism for Spent Fuel Dry Storage - EPRI-1001207, 2000.

II. Fracture Toughness Data for Zirconium Alloys and Its Application to Spent Fuel Cladding in Dry Storage," EPRI -1001287, 2000.

III. Creep Modeling and Analysis Methodology for Spent Fuel in Dry Storage, EPRI-1003135, November 2001.

IV. Response to Questions A.1-A.2 of RAIs dated April 17, 2002

V. Response to Questions A.3-A.4 of RAIs dated April 17, 2002

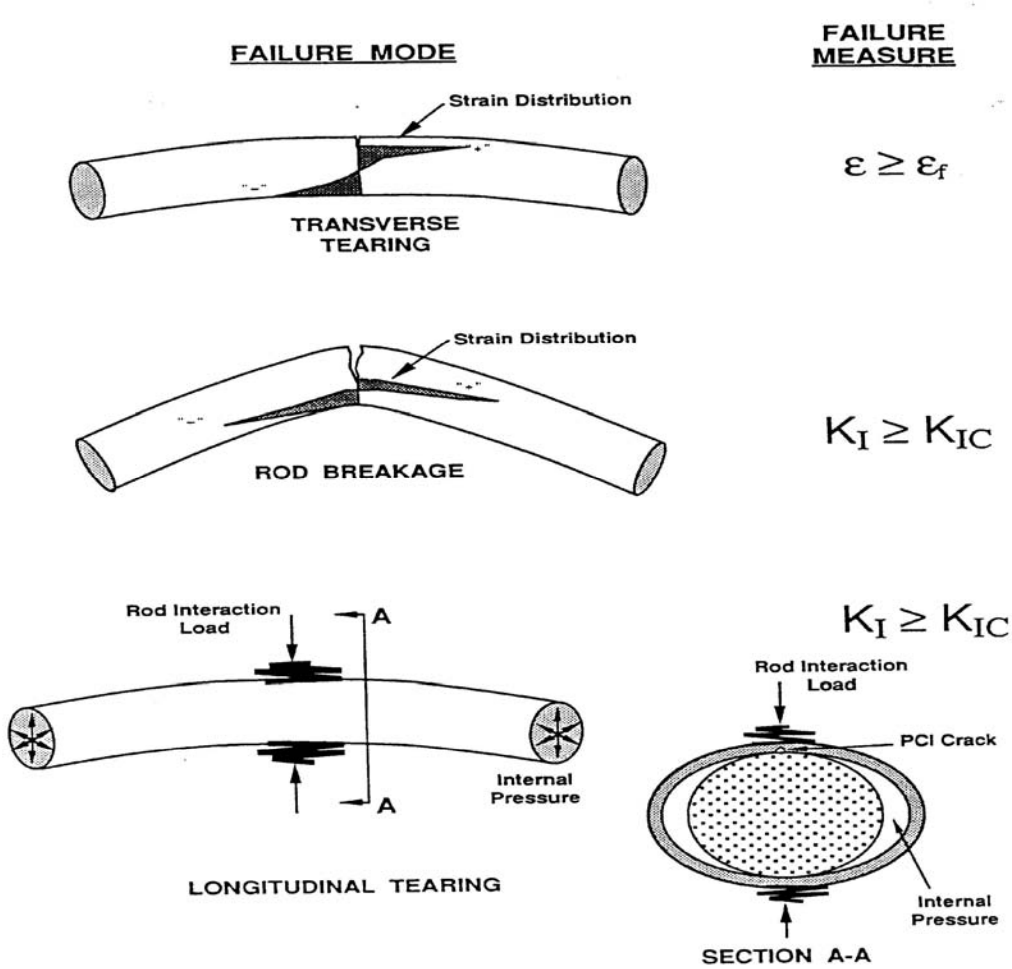
1. *"Analysis of High-Burnup Spent Fuel Subjected to Hypothetical Transportation Accidents, Part I and II," presented by Joe Rashid and Albert Machiels, NRC-Industry Meeting, Washington, DC, October 23, 2002.*

2. *"A Cladding Failure Model for Fuel Rods Subjected to Operational and Accident Transients," Progress Report, J. Rashid, R. Montgomery and W. Lyon of ANATECH, and R. Yang of EPRI.*

3. *"Hot Cell Examination of Extended Burnup Fuel from Calvert Cliffs-1," EPRI TR-103302-V2, July 1994.*

4. *"A Method for Determining the Spent Fuel Contribution to Transport Cask Containment Requirements," SANDIA Report, SAND90-2406, November 1992.*

5. *"Elastic-Plastic Deformation of a Nuclear Fuel Cladding Specimen under the Internal Pressure of a Polymer Pellet," Dufourneaud, et al., Fifth World Congress on Computational Mechanics, July 2002.*



Failure Mode

Mode-I, Extends to Mode II

Mode II

Mode III

Figure 1 – Fuel Rod Failure Modes

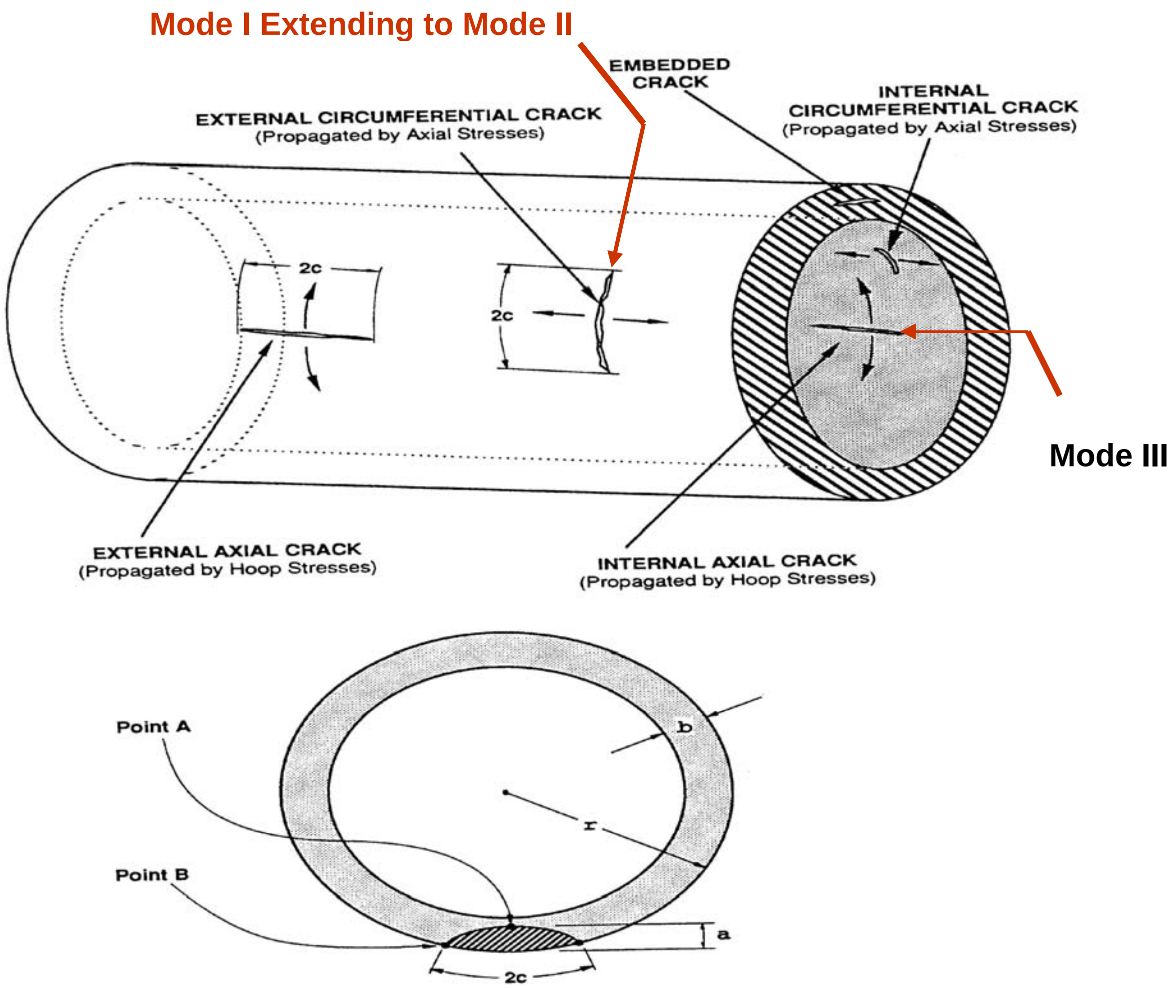


Figure 2 – Failure Modes and Potential Initiation Sites

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