A Review of the Critical Strain Energy Density (CSED) Model to Analyzing Reactivity Initiated Accidents (RIA) in High Burnup Fuel

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This is a review of the proposal submission by EPRI to NRC to use strain energy density as a failure criterion for high burnup fuel during an RIA. [1] This review identifies some concerns both in the theoretical basis of the CSED model and in its implementation for the study of RIA. It is concluded that, as presented, the CSED model for analyzing RIA induced fuel failures has basic concerns that need to be addressed and that the model failure predictions may be non-conservative.

1. General Comments

It should be pointed out that the attempt to analyze, unify and compare the integral tests, and separate effects tests both on irradiated materials and on unirradiated materials is a necessary effort that can lead to better understanding of the test results. This is not a simple endeavor, because of the complexity of the evolution of fuel and cladding microstructure during reactor exposure and of the physical phenomena associated with fuel rod response during an RIA. Thus, this review is not an attempt to counter such attempts, but rather, a constructive effort to contribute to the improvement of the overall description and modeling of the RIA response of the fuel.

The material conditions prevalent in the higher burnup cladding material during an RIA change during reactor exposure with the development of radiation damage, the occurrence of corrosion and hydriding, and the progressive bonding of the fuel and cladding. In addition to the cladding, the UO_2 fuel also evolves during in-reactor exposure leading to fuel swelling, pellet-cladding bonding, cracking and the development of the so-called “rim structure” near the outer diameter of the fuel pellet.

The response of the irradiated fuel to the insertion of a large amount of reactivity is also complex, and the survival of the cladding depends critically on the exact timing between the increase in temperature and the increase in the loading on the cladding caused by PCMI and fission gas release. [2] There are, as the authors mention, two possible regimes for cladding failure during an RIA. The first can occur early in the transient, if the fuel expands quickly against the cladding before the cladding temperature has substantially increased. If the cladding survives the initial transient, it can still fail in the high temperature regime by ballooning and rupture. The authors correctly focus on the low temperature failures as the most penalizing for the cladding, so the remainder of this review concerns itself with this type of failure.

The state of stress the cladding is submitted to during the RIA event is expected to be one between plane strain in the hoop direction and equal biaxial tension. [3] Because it is difficult to analyze the interaction of such states of stress with the microstructure of the irradiated and hydrided cladding, this problem does not lend itself to the easy formulation of an analytical failure criterion. Thus it is understandable that an empirical approach as chosen by the authors might work in cases where the full physical description of the phenomena is not available. Strain energy density may in fact be a useful parameter to formulate local failure initiation criteria. However, there are serious concerns, in both the theoretical basis for the formulation of CSED model and on the implementation of the CSED criterion within the current submission. Some of these specific questions are expressed in the following.

2. The CSED model

The authors propose that in order to predict low temperature RIA failures we calculate the strain energy density (SED), given by their equation 2.1 for the cladding material during the transient. This calculated value is compared to a critical strain energy density (CSED) value, which is a function of material variables and stress state, etc., and which is determined by the authors by fitting experimental data. The authors chose SED instead of other measures of failure because they believe it gives the smallest scatter in fitting the failure limit in the data (used to determine the CSED). The failure condition is then simply that

$$(SED)_{calc} > CSED\ (T,\ f,\ H,\ \ldots)$$

The authors apply this model to the known experimental results from mechanical tests in irradiated materials and derive master curves correlating CSED to the oxide thickness (and by implication to the overall sample H content) for both spalled and non-spalled oxides.

Concerns related to theoretical approach

This section comments on the specific problems of the basic formulation of the CSED concept, that is, how does it compare against simpler failure criteria, and whether it addresses the relevant physical phenomena and gives the correct answer.

i. **CSED model does not explicitly consider existing or potential flaws in its formulation.** The theoretical basis of the derivation of the SED model [4]from the J-integral approach of Rice [5]was criticized by Monerie, [6]who pointed out that the replacement of fracture mechanics analysis by an elasto-plastic calculation on the uncracked body is not suitable. Monerie also point out that the relationship derived by Rashid et al. (equation 1 in ref.) [4]between the J-integral and SED should exhibit an explicit dependence on crack length, which it does not. It has been shown experimentally that flaws of different sizes and origins (cracked hydride rims or blisters, thickness imperfections, etc.) can localize deformation such that the overall failure strain becomes small, although local strains are very high. In many situations flaw depth/flaw size is a critical parameter in determining the survivability of the cladding material. For example Jernkvist [7]mentions that SED fails to capture the effect of stress biaxiality in causing failures at cladding imperfections. Other studies have shown similar results of sensitivity of cladding to failure. [3, 8]
   The authors implicitly take the flaws into account by considering a data set such as shown in Figure 2-12 where such flaws exist in some samples. The authors separate this data set into data obtained from cladding with spalled and with non-spalled oxides. The non-spalled oxide data presumably contain some samples in which failure occurred because of presence of flaws, and thus implicitly flaws are considered, as stated by the authors on page 2-27. There are two problems with this approach: (i) the data set used

has to be representative of the conditions expected to be seen in reactors, to a degree that is likely not available in the current data, and (ii) this approach does not recognize that there are other possible sources of flaws beside blisters which do not require spallation (the hydride rim, thickness reductions from fretting or, possibly, from shadow corrosion in BWR, e.g.,). Thus by not explicitly introducing flaw size, the correctness of the predictions of the proposed model is completely dependent on how well the data set used to derive the CSED limits describes and incorporates the ductility decrease from these possible causes of flaws during an RIA. If other causes of flaws are identified, it is not possible to predict their influence on the failure of the material.
Thus, there are questions on the correctness of the theoretical formulation based on the J-integral. The implicit consideration of crack size approach used by the authors causes the prediction of the failure to be uncertain, because we do not know how representative and complete is the data set used. Thus, the model should explicitly consider flaw size in its formulation.

ii **CSED does not consider the failure mechanism.** This observation is similar to the preceding one. The types of failures observed can vary from a completely brittle failure such as appear to have occurred in Rep-Na1 to a failure in which a mode I crack initiates and localizes deformation in a small zone underneath the crack, to a shear instability caused by a thickness variation, or previous failure of a blister, to a mixed mode I and mode II crack propagating near a crack in the fuel. The model SED treats these different types of failures the same way. This causes two concerns: (i) in not identifying the failure mechanism how can the applicability of the model to real data be ensured? and (ii) different modes of failure can have different consequences for fuel dispersal. Thus, in simplifying the physics of the problem, the CSED model may overestimate failure strains because more penalizing failure mechanisms are not be taken into account.

iii. **CSED does not bring in additional physical description of phenomena or improve empirical correlation of the results when compared to strain based failure models.** A parameter such as strain energy density, combining stress and strain, should be used in replacement of the simpler strain-based criteria if it provides a simpler or more complete description of the phenomena. For example if the use of SED automatically took into account the variation of stress state, (i.e., if the material always failed at a constant value of SED, regardless of whether the material was being tested under uniaxial or biaxial tension for example), then CSED would be an improvement over simply using uniform or total strain as failure criteria. This is, in fact, not the case, as acknowledged by the authors who have had to introduce “knockdown” factors to account for the influence of stress state. In fact, to this reviewer it is not apparent that there is a physical phenomenon that is better explained by the use of CSED than stress or strain based approaches. [1]

As mentioned above, the authors invoke an empirical reason to use SED - less scatter in the fit of the data – but this is in fact not verified. Examining Figure 2-16, (uniform strain at failure), it is apparent that the scatter in this plot is much smaller than that in Figure 2-12 (CSED at failure).

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[1] One possible exception is that SED/CSED can treat failures in the elastic and plastic regimes in the same way, i.e. it is not necessary to switch from a stress based criterion to a strain based criterion in going from the elastic to the plastic regimes.

Thus CSED does not offer a more complete physical description of the phenomena than simpler models nor does it produce better data fits, so there is no compelling reason to use it in place of simpler failure criteria.

4. Concerns related with CSED model implementation in submission

The following comments do not address the validity of CSED model per se, but only whether it was applied correctly and consistently in the current submission, according to the stated designs of the authors.

iv. **Large scatter in the data used for determination of CSED leads to significant uncertainty in failure predictions**. In the model formulation, essentially all the physics of the phenomena involved are contained in the critical strain energy density parameter (CSED) which is derived from experimental data. Most of the data for the CSED determination are contained in Figure 2.12, page 2-32. The data in this graph is seen to be widely scattered (see below a comment for some of the possible reasons for this scatter). Even if one accepts that all the data in Figure 2.12 are applicable to the problem (see next paragraph), the data scatter is likely to cause problems in determining a unique fit. The proper way to derive a "best curve" from these data is by performing detailed statistical analysis, as possibly the authors have done, but any such differences between best curves are likely to be marginal, because of the large scatter. This means that there is not a good basis for the determination of a unique fit to the data. Thus, the scatter in the data means that any determination of CSED from this data and its associated failure prediction will necessarily have a significant associated uncertainty.

v. **The data scatter likely arises from different tests being put on the same graph.** In section 2.3.1.2 the authors give a few causes for the observed data scatter (material variability, inherent imperfections in testing procedures, variation in radiation damage, etc). In reality, the data scatter in fig. 2.12 could be attributed to the fact that the data shown are a mixture of uniaxial ring tests, burst tests and axial tests. One could expect that these tests would give different results. In fact the most logical separation of the data in Fig. 2-12 is by stress state or type of test rather than by whether spallation occurred or not. When the results in fig 2-12 are grouped by type of test, the burst test results (the most relevant to the situation of interest) are grouped towards the lower range of the SED failure values.

vi. **The experimental data set used in the determination of the CSED improperly mixes non-applicable mechanical tests with applicable tests.**
Given the biaxial stress states prevalent during RIA, and the direction of deformation being the hoop direction, data on uniaxial testing and on axial tension testing should not be used to predict failure, because the failure mechanism is different for these samples. For example, uniaxial hoop rings fail by thinning across the width of the sample (a mode of failure not available to cladding tubing) rather than through the sample thickness. [9, 10] The most relevant data for RIA (because of biaxial stress state and correct failure mechanism by through thickness deformation) is the burst data. Thus, a more defensible way to use the data is to base the fitting only on burst data, which, incidentally, would reduce the data scatter significantly. However, because burst data predict lower failure strains than when uniaxial tension and axial tension tests are included (see previous paragraph), performing a fit over only the more relevant results

will result in a lower CSED failure prediction. Thus including the additional uniaxial and axial data in the fit is non-conservative because the methodology used by the authors predicts more ductility than the cladding is likely to exhibit.

vii. **The knockdown factor to account for stress state is applied inconsistently.** As mentioned above, the SED formulation takes no explicit account of the state of stress except for the "knockdown factor" in the CSED, shown in equation 2.11. However, equation 2.11 was derived from a numerical fit of the plane strain data in reference. [11] As mentioned above, the state of stress of the cladding during RIA is likely to be somewhere between plane strain tension and equal biaxial tension. In the authors' formulation, equal biaxial deformation is implicitly assumed because significant fuel/cladding bonding is expected, and because fission gas release is not thought to be important. If the state of stress of the cladding during RIA is equal biaxial tension a different (and more penalizing) knockdown factor than that for plane strain tension would have to be assumed, if the data in reference [11] were used as a basis for the determination[2]. Thus, also in this case the failure strain limits calculated by the authors are non-conservative.

viii. **Role of hydride rims in degrading cladding ductility.** The authors separate out samples that contain spalled oxide and state that because of blister formation, these samples are more susceptible to failure. The argument is that if spalling can be eliminated from the fuel rods taken to high burnup, blisters will not form and thus the cladding will be less likely to fail. However, the presence of a hydride rim (which does not require oxide spallation) is as penalizing (if not more) than the presence of a blister of equivalent depth [12]. While localization phenomena in general are clearly a possible way whereby cladding ductility can be reduced, [7, 13]it is more likely that a cracked hydride rim causes cladding failure due to crack growth. The criterion for crack growth is sensitive to both crack depth (hydride rim layer thickness) and mode of cracking (e.g., mode I vs. mixed mode I/mode II).

The authors mention that hydride rim limited failures are only seen at room temperature (page 3.13), citing the NSRR tests. [14] It is difficult to verify the occurrence of these phenomena in integral tests because the temperature excursion that follows the reactivity insertion may modify the hydride distribution, but incipient cracks and localization phenomena are seen in various RepNa tests in Cabri where cladding temperatures are $\geq$ 280 C. In separate effects testing where phenomena can be more clearly identified, it is clear that hydride rim induced failures are also seen at 300 C (albeit with a different failure mode than at room temperature). Figure 1 shows metallography of fracture sections from separate effects testing at room temperature and 300 C. In both cases the hydride rim fails shortly after yielding of the substrate, which causes strain to localize under the cracked hydride rims. At room temperature the crack propagates along a plane normal to the maximum principal stress, while at 300 C, failure occurs by shear at 45 degrees to the principal stress direction. In both room temperature and 300 C tests it is seen that the thickness of the hydride layer is the

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[2] An additional point is that reference 11 may not be an appropriate basis for knockdown factors because it is based on (i) only room temperature behavior (mode I crack growth failure) and (ii) recrystallized Zr with random hydrides, many of those on grain boundaries.

critical factor in limiting failure strains. Thus by focusing only on spalled oxide we may be ignoring the degradation problems associated with the presence of hydride rims.

5. Additional Comments

Additional information is required in some instances and some minor points are also mentioned below.

ix. **How much spallation is too much?** It is not clear what is the limit for considering that a rod has spallation. The authors mention “enough spallation to affect mechanical properties” but how much of an effect is not spelled out. How much spallation should be considered detrimental? This needs to be clearly specified so that rods that are (according to the authors) unacceptably degraded can be kept out of reactors.

x. **More details on the experimental database should be furnished**. For example in Figure 2.13, the authors need to specify the details of the testing. As seen in the comments for Figure 2.12, the state of stress is important. In fact, the full data set used to derive the CSED curve should be made available as an appendix in the submission so that reviewers can assess the appropriateness of the experimental methods and analytical techniques used.

xi. **PCMI versus Fission gas loading;** In the authors formulation of the SED approach they assume PCMI as the main driving force for deformation but this is not completely clear, as the contribution of fission gas loading has been proposed also as a possible contributor. [14, 15]

6. Conclusions

In conclusion, there are concerns that need to be addressed both on the theoretical basis and on the implementation of the model to predict failure using CSED:

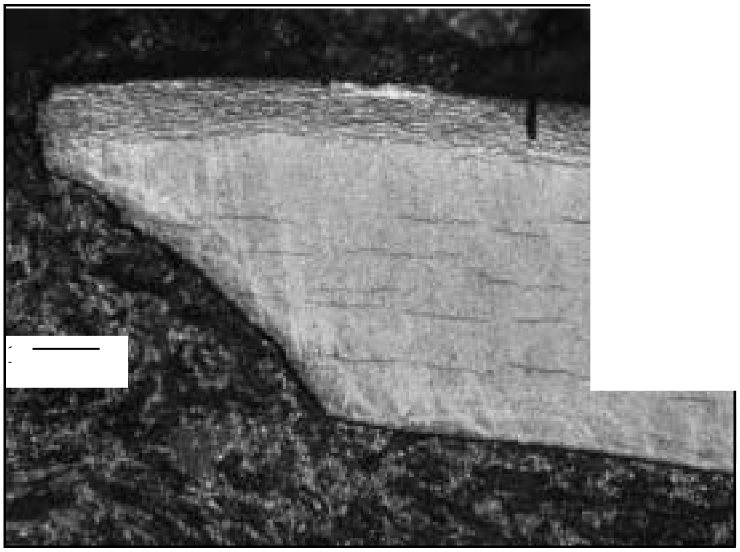
Theoretical Justification

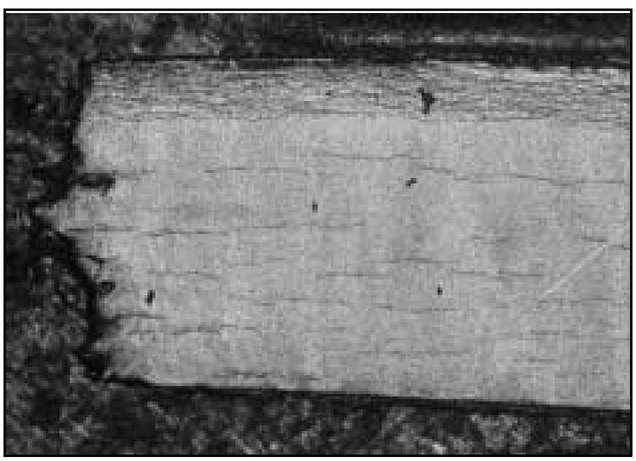
- There is not a clear compelling reason to use SED as the failure parameter, because it does not inherently provide a more complete physical description of the phenomena and exhibits more data scatter than uniform elongation.
- Both flaw size and failure mechanisms should be part of the formulation of failure criteria for cladding during RIA.
- There are questions on the derivation of the model from the J-integral approach.

Implementation

- In various instances during the implementation of the model the methodology used by the authors to analyze data and predict failure is seen to be non-conservative.
- The large scatter in the data used to derive CSED limits causes significant uncertainty in the failure predictions of the CSED model.
- The determination of CSED should be done using only experimental data obtained using stress states that are relevant for RIA.

- The presence of blisters is addressed by keeping spalled rods out, but it is not clear how applicable this determination of the failure limit is beyond the current data set.
- More consideration needs to be given to the role of hydride rims in degrading cladding ductility. This is because (i) they are present at lower exposure levels than blisters, (ii) do not require oxide spallation to be formed, and (iii) may be at least as penalizing to cladding ductility for a given depth.





a)(b)

Figure 1: Transverse fracture profiles of cladding with a hydride rim tested at (a) room temperature and (b) 300°C, both showing hydride rim induced failure.

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