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A Mechanistic Deformation Model of TRU Metal Dispersion Fuel for HYPER

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Abstract

An in-reactor performance computer code for a dispersion is being developed in the conceptual design stage of blanket fuel for HYPER. In this paper, a mechanistic deformation model was developed and the model was installed into the DIMAC program. The model was based on the elasto-plasticity thoery and power-law creep theory. The preliminary deformation calculation results for (TRU-Zr)-Zr dispersion fuel predicted by DIMAC were compared with those of silicide dispersion fuel predicted by DIFAIR. It appeared that the deformation levels for (TRU-Zr)-Zr dispersion fuel were relatively higher than those of silicide fuel, and the dispersion fuel performance may be limited by swelling. Therefore, some experimental tests including inpile and outpile experiments are need for clarifing the integrity and the material properties of the inter-metallic fuel core.

1. Introduction

Renewed interest in the dispersion metallic fuel for ADS(Accelerator Driven System) has arisen in the USA and Korea[1,2]. TRU-Zr metal alloy or (TRU-Zr)-Zr dispersion fuel is being considered as a blanket fuel for HYPER (HYbrid Power Extraction Reactor). In the case of dispersion fuel, the particles of the TRU-Zr metal alloy are dispersed in a Zr matrix. A blanket rod is made of sealed tubing containing actinide fuel slugs in columns. The blanket-fuel cladding material is ferritic-martensitic steel.

Performance analysis in fuel rod design is essential to assure adequate fuel performance and integrity under irradiation conditions. This paper represents an attempt to develop a physical model to efficiently predict the in-reactor deformation of the (TRU-Zr)-Zr dispersion fuel rod according to its power history. Modeling efforts have been made to develop DIMAC (A DIspersion Metallic fuel performance Analysis Code)[3]. This code will be used for simulating operational limits of dispersion metal fuels under steady state conditions, and it will also be used as a design tool for the HYPER fuel.

In this paper, a mechanistic deformation model was developed and the model was installed into the DIMAC program. The model was based on the elasto-plasticity thoery and power-law creep theory. The preliminary deformation calculation results for (TRU-Zr)-Zr dispersion fuel predicted by DIMAC were compared with those of silicide dispersion fuel predicted by DIFAIR[4].

2. Fuel Element Deformation Model

In this fuel element deformation modeling, the system is assumed to be axisymmetric, and the cylindrical coordinate system (r, θ, z), where r, θ, z are radial, tangential, and axial directions respectively, is used. However, since this model is a one-dimensional model, no variations in tangential and axial direction have been considered. Also, since the fuel meat cladding composite is assumed to be a continuum, no central void can form during irradiation.

2.1 Strain Rate Distribution

Since the fuel element considered in this modeling is assumed as a continuum, the deformation has to obey compatibility equations. In the cylindrical coordinate system (r, θ, z), the compatibility equation is given by[5]

$$\frac{d\varepsilon_\theta}{dr} + \frac{\varepsilon_\theta - \varepsilon_r}{r} = 0 , \qquad (1)$$

where, ε_θ and ε_r are hoop strain and radial strain, respectively.

Differentiating the strain components with time(t), the strain rate components labeled $\dot{\varepsilon}_\theta$ and $\dot{\varepsilon}_r$, also satisfy the same form of compatibility equation. That is

$$\frac{d\dot{\varepsilon}_\theta}{dr} + \frac{\dot{\varepsilon}_\theta - \dot{\varepsilon}_r}{r} = 0 . \qquad (2)$$

Replacing the two terms in Eq. (2) with these finite difference equations, then Eq. (2) becomes

$$p\dot{\varepsilon}_\theta^{i+1} + q\dot{\varepsilon}_r^{i+1} = h_e . \qquad (3)$$

where, $p = \dfrac{1}{r^{i+1} - r^{i}} + \dfrac{1}{2r^{i+1}}$, $q = -\dfrac{1}{2r^{i+1}}$, $h_e = \dfrac{\dot{\varepsilon}_\theta^{i}}{r^{i+1} - r^{i}} - \dfrac{\dot{\varepsilon}_\theta^{i} - \dot{\varepsilon}_r^{i}}{2r^{i}}$.

Since isotropic fuel core swelling and isotropic thermal expansion are assumed, the strain rate components can be expressed as

$$\dot{\varepsilon}_r = \dot{\varepsilon}^{th} + \dot{\varepsilon}^{s} + \dot{\varepsilon}_r^{c} \tag{4-a}$$

$$\dot{\varepsilon}_\theta = \dot{\varepsilon}^{th} + \dot{\varepsilon}^{s} + \dot{\varepsilon}_\theta^{c} . \tag{4-b}$$

where, $\dot{\varepsilon}^{th}$ is the thermal expansion strain rate, $\dot{\varepsilon}^{s}$ is swelling strain rate, and $\dot{\varepsilon}_r^{c}$ and $\dot{\varepsilon}_r^{\theta}$ are creep strain rate components.

Creep deformation and instantaneous plastic deformation occur under constant volume condition (unlike elastic deformation, swelling, or thermal expansion). The present model assumes that creep does not occur along the axis direction (z-direction), $\dot{\varepsilon}_z^{c}$. So the components of creep strain are related by the constant volume condition : $\dot{\varepsilon}_r^{c} + \dot{\varepsilon}_r^{\theta}=0$.

Using this constant volume condition and equations (3, 4-a), and (4-b), the θ components of creep strain rate at (i+1)th annulus can be expressed as

$$\dot{\varepsilon}_\theta^{c,i+1} = \frac{h_e - (p+q)(\dot{\varepsilon}^{th,i+1} + \dot{\varepsilon}^{s,i+1})}{(p-q)} . \tag{5}$$

With the constant volume condition, the radial component of creep strain rate at *(i+l)*th annulus is given by

$$\dot{\varepsilon}_r^{c,i+1} = -\dot{\varepsilon}_\theta^{c,i+1} . \tag{6}$$

Each total strain rate component is the sum of thermal strain rate, swelling strain rate, and creep strain rate. These strain rates are calculated by separate models. The thermal strain rate is calculated under the assumption of isotropic thermal expansions. The thermal expansion strain rate are given by;

$\dot{\varepsilon}_{th} = \alpha_{(TRU\text{-}Zr)\text{-}Zr} T$ for fuel meat (7-a)

$\dot{\varepsilon}_{th} = \alpha_{HT9} T$ for cladding (7-b)

where, $\alpha_{(TRU\text{-}Zr)\text{-}Zr}$=fuel meat thermal expansion coefficient

α_{HT9}= cladding thermal expansion coefficient.

The swelling strain rate is evaluated by a fuel swelling model, in which the swelling of the fuel comprises three major components:

(i) volume change due to thermal-chemical reactions at the interfaces of TRU-Zr fuel particles and zirconium matrix, (ii) volume change due to nucleation and the growth and coalescence of fission gas bubbles, and (iii) volume change due to solid fission products and due to the phase change by temperature gradient and depletion of TRU in TRU-Zr compound.

The equivalent strain rate is expressed as follows.

$$\dot{\bar{\varepsilon}}^{c} = 0.25 \times [4.01823 \times 10^{-6}\, T + (-1.2266 \times 10^{-3})]\, \sigma. \tag{8}$$

where, $\dot{\bar{\varepsilon}}^{c}$: effective strain rate, (% per dpa)

T : temperature in °C

σ: effective stress in Mpa.

With the constant volume condition ($\dot{\varepsilon}_r^c + \dot{\varepsilon}_r^\theta = 0$) and with the assumption of $\dot{\varepsilon}_z^c = 0$, the equivalent plastic strain rate is given by

$$\dot{\bar{\varepsilon}}^{c} = \frac{\sqrt{2}}{3}\left[(\dot{\varepsilon}_r^c - \dot{\varepsilon}_\theta^c)^2 + (\dot{\varepsilon}_\theta^c)^2 + (\dot{\varepsilon}_r^c)^2\right]^{1/2} = \frac{2}{\sqrt{3}}\dot{\varepsilon}_\theta^c . \tag{9}$$

2.2 Stress Distribution

In the cylindrical coordinate system (r, θ, z), the force equilibrium conditions become a governing equation for stress distribution by eliminating shear stresses and θ and z-derivatives:

$$\frac{d\sigma_r}{dr} + \frac{\sigma_r - \sigma_\theta}{r} = 0 . \tag{10}$$

Regarding that the stress components of the *i*th annulus, σ_r^i and σ_θ^i, and radial position, r^j and r^{j+1}, are known, the above equation can become a relationship for σ_r^{i+1} and σ_θ^{i+1} by finite difference equation as follows;

$$p\sigma_r^{i+1} + q\sigma_\theta^{i+1} = \eta_e \tag{11}$$

where, p and q are defined in Eq. (3), and

$$\eta_e = \frac{\sigma_r^i}{r^{i+1} - r^i} - \frac{\sigma_r^i - \sigma_\theta^i}{2r^i} . \tag{12}$$

Since two dimensional deformation in the r-θ plane is assumed, the stress components are related by Tresca equation; $\sigma = \sigma^\theta - \sigma^r$, where σ is equivalent stress. At each annulus, the equivalent stress can be evaluated by the creep rate equation. Inserting the Tresca equation into Eq. (12), the hoop stress at *(i+l)*th annulus can be expressed by

$$\sigma_\theta^{i+1} = \frac{\eta_s + p\sigma^{i+1}}{(p+q)} . \tag{13}$$

From the Tresca equation, then, the radial stress at *(i+l)*th annulus is given by

$$\sigma_r^{i+1} = \sigma_\theta^{i+1} - \sigma^{i+1} . \tag{14}$$

2.3 Total strain

Total strain components are obtained using the constitutive equations. Each strain component comprises five strain terms. Letting ε_θ and ε_r be the total strain components in the radial and hoop directions, the constitutive equations are expressed by

$$e_r = As_r - B(s_q + s_z) + e^{th} + e^s + e_r^c \qquad (15\text{-a})$$

$$e_q = As_q - B(s_z + s_r) + e^{th} + e^s + e_q^c . \qquad (15\text{-b})$$

where, ε^{th} is the strain due to isotropic thermal expansion, ε^s the swelling strain component, and ε_r^c and ε_θ^c the creep strain components.

These three kinds of strain are obtained by summation of the strain increments ($\varepsilon\Delta t$) of respective time (burnup) intervals. Further, on the basis of general linear elasticity and Hencky' s total strain theory, the coefficient A and B (and H) are given by

$$A = \frac{1}{E} + \frac{2}{3}H \qquad (16\text{-a})$$

$$B = \frac{n}{E} + \frac{1}{3}H \qquad (16\text{-b})$$

$$H = \frac{3}{2}\frac{e^p}{s} = \frac{3}{2}\frac{(s/K)^{1/n}}{ns} . \qquad (16\text{-c})$$

Here, E is Young's modulus (88.6 GPa for fuel meat and 70 GPa for cladding), v is Poisson ratio (0.3 for fuel meat and 0.33 for cladding). Power-law hardening is assumed for the stress-plastic strain relationship; $\sigma = K(\varepsilon^p)^n$, here is the instantaneous equivalent plastic strain, K is the strain coefficient (100 for both fuel meat and cladding), and n is the strain hardening coefficient (0.2 for both fuel meat and cladding).

If using the strains calculated by the above Eq. (15), the radial displacement(u) at each annulus and element volume change can be evaluated. The radial displacement and hoop strain are related by an equation of $U=r\varepsilon_\theta$. The element volume change, in % is defined by 100 x $((r_o+U_o)^2-r_o^2)/r^2$, where r_o is the outer radius of cladding before deformation, and U_o is the displacement at cladding outer surface.

At the center of fuel element (r=0) a radial gradient is zero. This condition can be applied strain rate and stress distributions. Applying these conditions to the compatibility Eq. (2) and force equilibrium Eq. (10), the stress and strain components have to satisfy the following relationships.

$$Ý_r = Ý_\theta \qquad (17\text{-a})$$

$$\sigma_r = \sigma_\theta . \qquad (17\text{-b})$$

At the interface between fuel meat and cladding the following conditions can be derived from the continuity condition of displacement and force:

$$Ý_\theta^{meat} = Ý_\theta^{clad} \qquad (18\text{-a})$$

$\sigma_r^{clad} = \sigma_r^{clad}$. (18-a)

The boundary condition applied at fuel element surface is that the radial stress is balanced with the coolant pressure (P_{fc} =0.3 MPa).

$\sigma_r = -P_{fc}$. (19)

This boundary condition will be used as the criterion for the convergence of solution in iterative calculation. Iterative calculation would be stopped if the calculated radial stress at cladding surface satisfies the convergence criterion.

$\vert$Radial stress at cladding surface + coolant pressure$\vert$ ≤ 0.02Mpa

3. Results and discussion

Figure 1 illustrates the distribution of radial strain, hoop strain, and swelling strain for fuel rod with (TRU-Zr)-Zr and (U_3SI)-Al fuel cores at a burnup of 10at% and the pin peak power of 21.6kW/m. All strain components are almost constant in the fuel cores. At cladding, hoop strain decreases with distance, whereas radial strain increases with distance. Since plastic deformation at cladding is subject to a constant volume condition, radial and hoop strains have reversed signs.

Comparing strains in figure 1, it is estimated that the deformation by swelling within fuel meat is very large for both fuels, and the major deformation mechanism at cladding is creep. And the swelling strain is almost constant within the fuel meat.

Figure 2 shows radial displacements versus positions. Within the fuel meat the radial displacement increases almost linearly with distance from the center. This fact coincides with the uniform swelling strain distribution. The cladding deformation is the creep deformation which is subject to constant volume conditions. This means that the cladding thickness would decrease as the meat swelling increases, although the radius of cladding increases. Thus the radial displacement decreases as the distance from center increase, and peak displacement is at the fuel meat-cladding interface. The displacement for (TRU-Zr)-Zr was relatively higher than that of (U_3SI)-Al. In Figure 2, the radial displacement is about 0.057mm at the cladding outer surface for (TRU-Zr)-Zr, and about 3.5% volume change (expansion) is calculated from this value. The radial displacement is about 0.029, 0.053mm at the cladding outer surface for (U_3SI)-Al at coolant temperature 414K and 613K, and about 1.834%, 3.228% volume change (expansion) is calculated from this value, respectively.

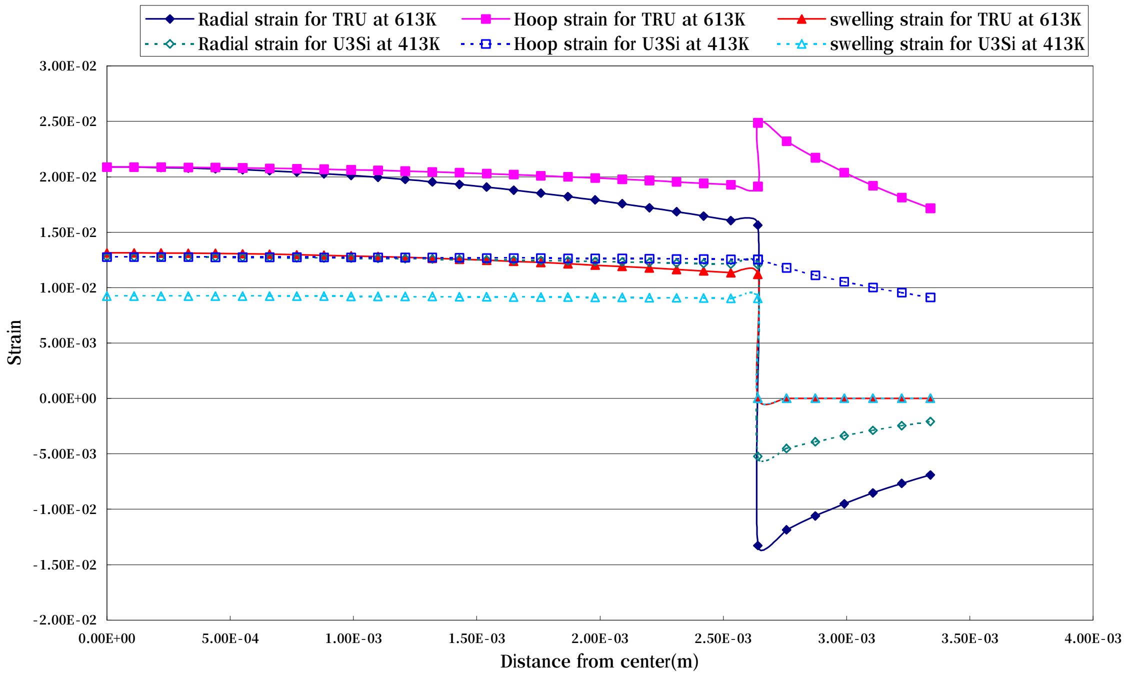

Fig. 1. Strain distribution at the burnup of 10at.%

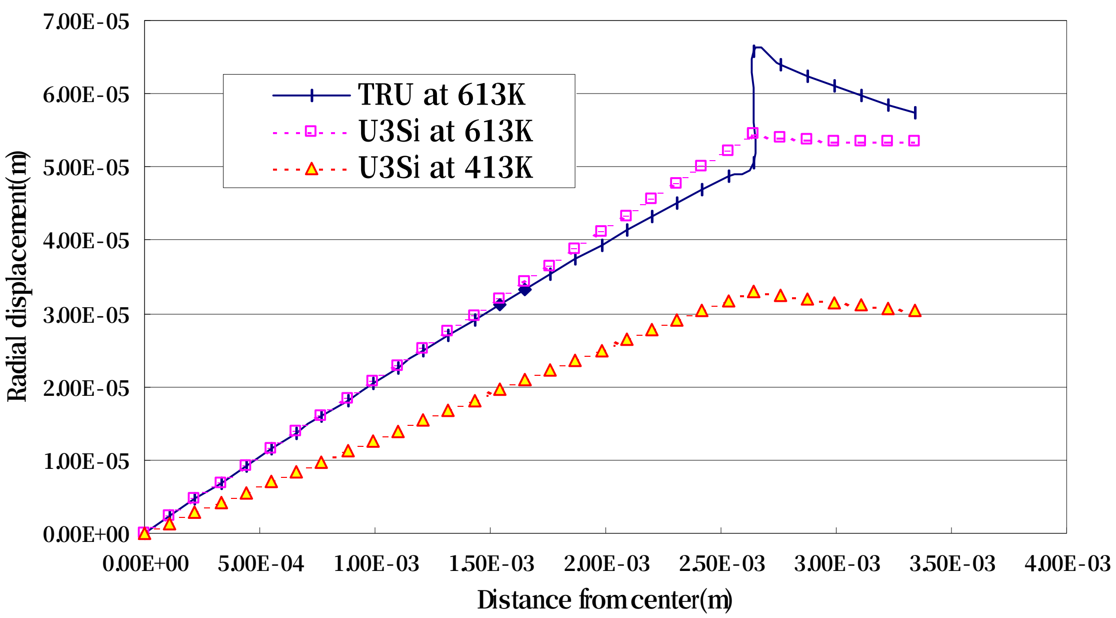

Fig. 2. Radial displacement versus the distance from fuel center at the burnup of 10at.%

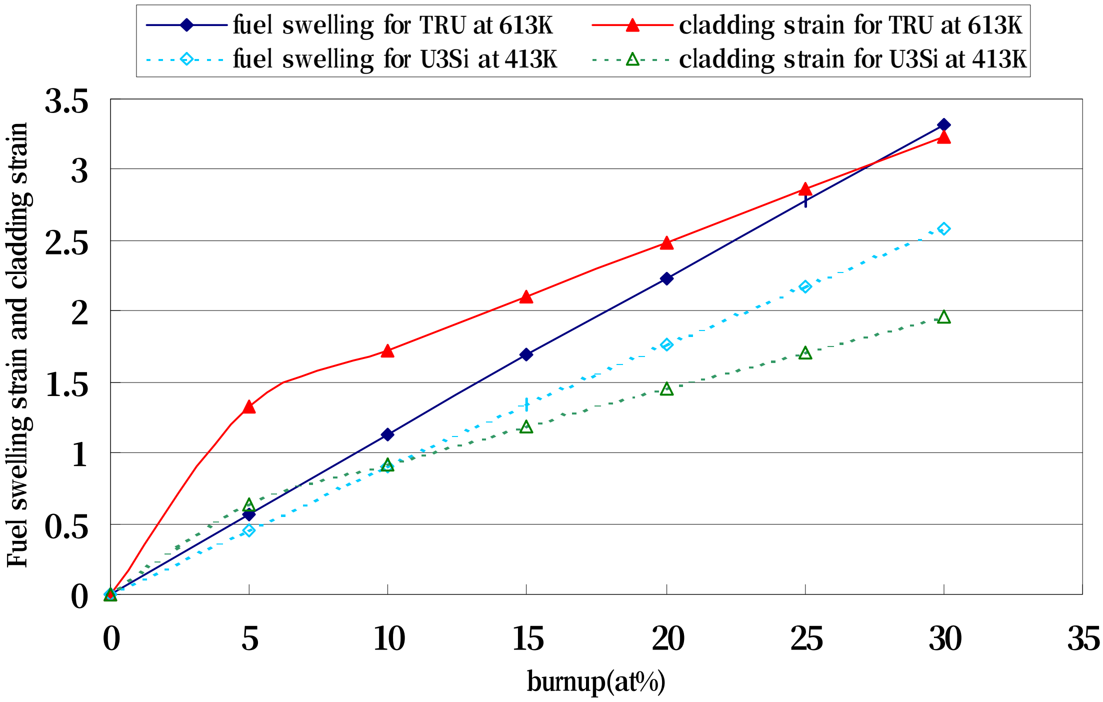

Fig. 3. Swelling and cladding strain for 45wt%(TRU-10Zr)-55wt% dispersion fuel as a function of burnup

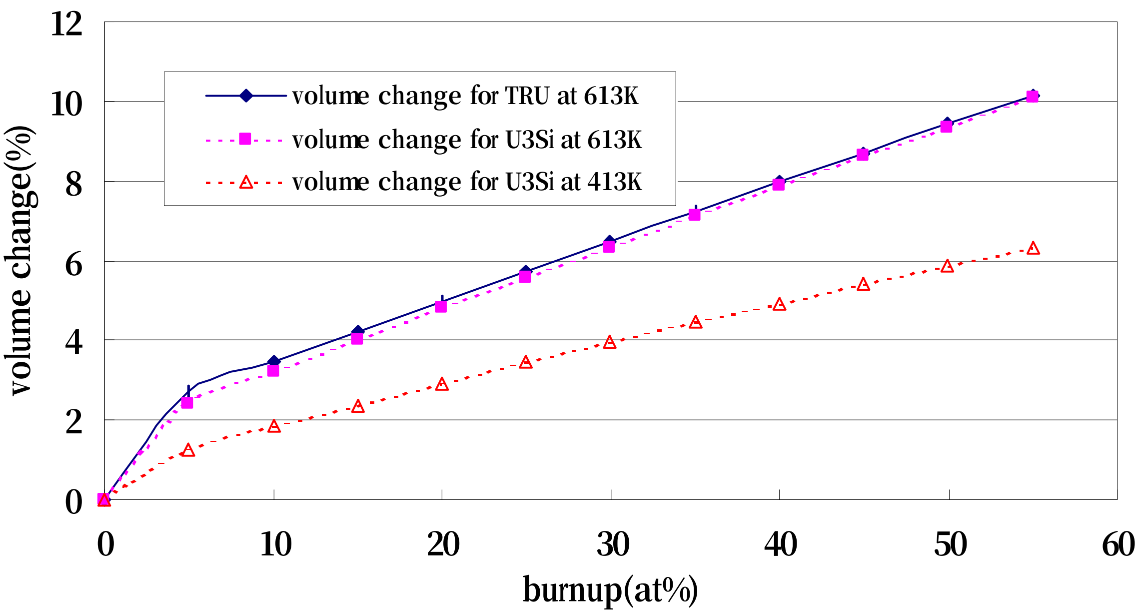

Fig. 4. Volume change for 45wt%(TRU-10Zr)-55wt% dispersion fuel as a function of burnup

Figure 3 shows the swelling and cladding strain for 45wt%(TRU-10Zr)-55wt%Zr dispersion fuel as a function of burnup. The fuel swelling value increase linearly with burnup, and shows about 3.5% swelling at 30at% burnup. The value of cladding strain is about 3.2% at 30at%. As the design requirement for cladding is assumed about 3% of cladding strain, a detailed review for cladding strain should be needed.

Figure 4 shows the volume change for 45wt%(TRU-10Zr)-55wt%Zr dispersion fuel as a function of burnup. The predicted volume change does not exceed 10% in the burnup range up to 50at%.

4. Conclusion

An in-reactor performance analysis computer code for blanket fuel is being developed at the conceptual design stage of blanket fuel for HYPER. In this paper, a mechanistic deformation model was developed and the model was installed into the DIMAC program. The model was based on the elasto-plasticity thoery and power-law creep theory. The preliminary deformation calculation results for (TRU-Zr)-Zr dispersion fuel predicted by DIMAC were compared with those of silicide dispersion fuel predicted by DIFAIR.

It appears that the deformation by swelling within fuel meat is very large for both fuels, and the major deformation mechanism at cladding is creep. The swelling strain is almost constant within the fuel meat, and is assumed to be zero in the cladding made of HT9.

It is estimated that the deformation levels for (TRU-Zr)-Zr dispersion fuel were relatively higher than those of silicide fuel, and the dispersion fuel performance may be limited by swelling. But the predicted volume change of the (TRU-Zr)-Zr dispersion fuel does not exceed 10% up to the burnup range 50at%. The value of cladding strain is about 3.2% at 30at%. As the design requirement for cladding is assumed about 3% of cladding strain, a detailed review for cladding strain should be needed. Therefore some experimental tests including inpile and outpile experiments are needed for clarifing the integrity and material properties of the inter-metallic fuel core.

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