Calculations of Fuel Rod Cladding Deformation for a Reactor Plant Wwer-1000 Under LB Loca

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ABSTRACT

To specify the thermo-mechanical models of fuel rod cladding behaviour and to determine the degree of conservatism in the calculational safety justification a complex of calculations and experimental work was made resulting in development of new fuel rod computer codes in axisymmetric (TVEL-3) and non-axisymmetric (TVEL-3/2) approaches. The developed fuel rod computer codes are used for solution of related problems of thermo-physical and thermo-mechanical behaviour of fuel rods in the core. For consideration of interrelation of process of creep and accumulation of damages the energy version of theory of creep and strength is applied. Verification of numerical models of fuel rod cladding ballooning was accomplished on the basis of tests of single simulators of fuel rods and fuel rod assemblies performed at test benches of OKB "Gidropress" and PARAMETER-M.

KEY WORDS: reactor core, fuel rod, creep, damage, ballooning, related problem, equivalent strain, axisymmetric strain, non-axisymmetric strain, orthotropy, FEM.

INTRODUCTION

In justification of reactor plant (RP) safety under accident the calculational determination is made of temperature and mechanical condition of fuel rod cladding in the reactor core. In the regulatory document, ref. [1], the maximum design limits are established for limitation of temperature of fuel rod cladding and the degree of their oxidization. Therefore the main objective of safety justification is a correct calculational simulation of the associated processes of fuel rod heat conduction and of heat transfer of fuel rod and coolant on the one hand, and thermo-mechanical behaviour of fuel rod cladding, on the other hand.

Under the conditions of LB LOCA the reduction in heat removal from fuel rods occurs. The resulting pressure differential on fuel rod cladding, in combination with high temperature, leads to occurrence of tensile stresses in fuel rod cladding that intensifies the creep processes. With the corresponding level of temperature the local ballooning of fuel rod cladding could occur. In general case the fuel rod ballooning gives rise to flow area pressure loss and reduction in heat removal from fuel rods. At high temperatures the fuel rod cladding material oxidization occurs that influence its mechanical properties. At high-temperature creep a break of fuel rod cladding could be expected within the period before the core flooding.

Calculational model of the core of WWER-1000 RP for the analysis of design basis accident of LB LOCA type according to thermohydraulic computer code TRAP comprises several calculated channels into which the fuel rods with similar power are combined. Description of thermal and thermo-mechanical behaviour of fuel rod in the calculated channel is made with the help of computer codes TVEL-3 and TVEL -3/2. In these codes the solutions of the related problem of thermo-mechanical and thermo-physical behaviour of fuel rod are realized within the frame of numerical integration of the problem equations versus time and the joint consideration of creep and damage processes.

The well-known Russian computer codes RAPTA-5 and PULSAR+ are used, as a rule, for solution of non-related problems of fuel rods behaviour under accidents. To determine the conditions of fuel rod cladding break the strain criterion is applied.

AXISYMMETRIC MODEL OF DEFORMATION

The strained state of fuel rod cladding in axisymmetric approach can be determined from solution of a system of nonlinear differential equations describing the high strains of orthotropic axisymmetric thin-walled cladding. With high strains the cladding deflections could be compared to its initial sizes, therefore in the course of straining a change in the cladding sizes is considered. The stressed state is two-dimensional because σ_r is small in comparison with σ_t and σ_m and it can be neglected. In consideration of axisymmetric ballooning there are no tangential stresses in meridional and circumferential directions of cladding.

Axisymmetric pattern of local ballooning can be built on the basis of equilibrium equations

$$p \cdot \pi \cdot r_0^2 = 2 \cdot \pi \cdot r \cdot h \cdot \sigma_m \cdot \sin\theta \,, \tag{1}$$

$$\sigma_m \cdot r \cdot \frac{d\theta}{dS} + \sigma_t \cdot \sin\theta = \frac{p \cdot r}{h} \,, \tag{2}$$

differential relations of the running radius versus an angle of the tangent slope to the meridian (dS- element of the clad meridian arc)

$$\frac{dr}{dS} = \cos\theta \tag{3}$$

and relations between increments of strains in meridional $d\varepsilon_m$, circumferential $d\varepsilon_t$ and radial $d\varepsilon_r$ directions versus increments of the arc length, radius and thickness

$$d\varepsilon_m = \frac{\Delta(dS)}{dS}, d\varepsilon_t = \frac{dr}{r}, d\varepsilon_r = \frac{dh}{h} \tag{4}$$

As the anisotropy of fuel rod cladding deformations is of orthotropic character, the relations of rates of creep strains in meridional and circumferential directions of ballooning of axisymmetric thin-walled cladding according to ref. [2] are of the following form:

$$\begin{aligned} \xi_m^c &= \frac{3}{2 \cdot (F+G+H)} \cdot \frac{\xi_e^c}{\sigma_e} \cdot \Big(H \cdot (\sigma_m - \sigma_t) + G \cdot \sigma_t\Big); \\ \xi_t^c &= \frac{3}{2 \cdot (F+G+H)} \cdot \frac{\xi_e^c}{\sigma_e} \cdot \Big(F \cdot \sigma_m + H \cdot (\sigma_t - \sigma_m)\Big); \end{aligned} \tag{5}$$

where stress intensities and creep rates are calculated by the formulae:

$$\sigma_e = \sqrt{F \cdot (\sigma_t - \sigma_m)^2 + G \cdot \sigma_m^2 + H \cdot \sigma_t^2} \tag{6}$$

NON-AXISYMMETRIC MODEL OF DEFORMATION

To describe the process of non-axisymmetric ballooning the FEM us used. The considered section of fuel rod cladding is divided into triangle finite elements by the regular scheme. The relationship connecting the rates of nodal displacements $\{q\}$ with the strain rates over the volume of finite element is determined by linear transformation

$$\{\xi\} = [B] \cdot \{q\} \tag{7}$$

Matrix [B] does not depend on coordinates and, consequently, the stressed-strained state over the volume of finite element is uniform. This prevent the necessity of numerical integration when calculating the stiffness matrix of elements. Relationships between the rates of strains $\{\xi\}$ and stresses are of the following form

$$\{\sigma\} = \frac{\sigma_e}{\xi_e} \cdot [D] \cdot \{\xi\}, \tag{8}$$

where $\{\sigma\} = \{\sigma_m \quad \sigma_t \quad \tau_{mt}\}^T$ - vector of effective stresses;

[D]- square matrix depending on anisotropy coefficients only.

Matrix [D] is of the following form

$$[D] = \begin{bmatrix} \frac{H+F}{HG+HF+FG} & \frac{H}{HG+HF+FG} & 0 \\ \frac{H}{HG+HF+FG} & \frac{H+G}{HG+HF+FG} & 0 \\ 0 & 0 & \frac{1}{N} \end{bmatrix} \tag{9}$$

Finite element stiffness matrix in global coordinate system is calculated with the help of congruent transformation

$$[k_G] = [L]^T \cdot [k_L] \cdot [L] \tag{10}$$

After calculation of stiffness matrixes of separate elements in global coordinate system the principles of possible displacement is applied to the whole cladding section under consideration. Finally the basic matrix FEM equation is obtained

$$[K]\{V\}=\{R\} \tag{11}$$

where {V} and {R} – vectors of velocities of displacement and forces in nodes of finite element mesh; [K] – stiffness matrix of the whole cladding section.

Matrix equation (11) is a system of nonlinear ordinary differential equations with many unknowns because the matrix stiffness coefficients [K] depend not only on nodal coordinates but also on the time history of change in stressed-strained state of finite elements in the course of their loading, as well as on velocities of nodal displacements at the given time moment governing the coefficient σ_e/ξ_e in equations (8). Components of vector {R} also depend nonlinearly on velocities of nodal displacements. Thus, the system of equations (12) for calculation of high-temperature creep of claddings can be written down in the following form

$$\left[K\left(\bar{a}_i, \left\{\sigma_e/\xi_e\right\}\right)\right] \cdot \{V\} = \{R(\{V\})\}, \tag{12}$$

and the solution is performed by linearization by increments of strains with the time integration step using Euler's scheme

$$\Delta\varepsilon^c_{mk} = \xi^c_{m,k-1} \cdot \Delta t_k, \quad \Delta\varepsilon^c_{tk} = \xi^c_{t,k-1} \cdot \Delta t_k \tag{13}$$

ENERGY THEORY OF CREEP AND DAMAGE

In paper of ref. [3] a description is given for studies of preferred orientation alloy Э110 within the temperature interval of 295-1475K to reveal the specific features of plastic strain and to determine the anisotropy parameters of plastic strain which can be used for prediction of behaviour of claddings during their heating up to temperature of 1500 K. These studies showed that anisotropy of the alloy plastic strain decreases with increase in temperature though its decrease to 625 K is insignificant and only at higher temperature it drops sharply. At temperature of 1275 K and higher the alloy becomes isotropic.

The running state of the alloy anisotropy can be evaluated with the help of anisotropy parameters F, G, H, included into the governing Hill's yield condition for anisotropic material, ref [2]. The mentioned anisotropy parameters were determined experimentally during one-layer tests of flat specimens cut out of tubes in directions z and θ. According to the data presented in ref. [3] it can be noted that the alloy anisotropy decreases with increase in temperature and decrease in difference between F, G, H is indicative of this. At temperature of 1100-1200 K anisotropy disappears and anisotropy parameters become equal to 0.5 .

To describe the interrelated processes of creep and damage the energy version of theory of creep and strength is used. As the structural parameter it is reasonable to use parameter ω reflecting accumulation of damages scattered over the body volume, ref.[3], that is equal to ω=0 at the initial time moment, and to ω=1 at the moment of damage.

It is confirmed experimentally that the course of deformation processes with similar intensity of stresses and specific dissipation work A=A(t) does not depend on the type of stressed state and prehistory of loading, that is, it is assumed that the specific dissipation work, accumulated by the moment of damage, A_* is a constant value typical for each material and not depending on the level of acting loads and on the character of their variation, but having the constant value in rather wide range, ref. [2]. Thus, the damageability parameter ω is of specific mechanical sense using the specific dissipation work A as the parameter. Considering that in case of damage ω=1, we have

$$\frac{A(t)}{A} = \omega(t); \qquad \int_0^t \frac{W dt}{A_*} = \int_0^t \dot{\omega} dt; \qquad \dot{\omega} = \frac{\sigma_e \cdot \xi^c_e}{A_*} \tag{14}$$

where $\sigma_e \cdot \xi^c_e = \sigma_{ij} \cdot \xi^c_{ij}$.

T – absolute temperature, K.

Kinetic equations of creep and damage for the assigned temperature interval are of the following form

$$\begin{cases} \xi^c_e = \dfrac{k_0 \cdot \exp\left(-\dfrac{Q_c}{RT}\right) \cdot \sigma^n_e}{1-\omega} \\ \dot{\omega} = \dfrac{\sigma_e \cdot \xi^c_e}{A_*} \end{cases}, \tag{15}$$

where k_0 – a factor obtained by the results of tests; Q_c – specific activation energy; R – Boltzmann constant.

DETERMINATION OF CHARACTERISTICS OF CREEP AND DAMAGE OF ZIRCONIUM ALLOYS BY THE RESULTS OF EXPERIMENTS

At test benches of OKB "Gidropress" the tests were performed for the tubular specimens of fuel rod cladding of alloys Э110 and Э635 under temperature and force conditions typical for LB LOCAs. The tests were performed with the temperature range from 680 to 900 °C and at pressure differentials from 2 to 12 MPa, ref. [4]. During the tests the continuous recording of circumferential strain of fuel rod cladding with the use of video filming.

Determination of parameters k, n, A_* of systems of equations (15) for alloys Э110 and Э635 by the results of experiments was made by solution of problems of ballooning according to axisymmetric approach (1) – (6). With regard for dependence $\sigma_\theta = 2\sigma_z$ and (5), (6) the rates of logarithmic creep strains for anisotropic material are determined by the relationships:

$$\bar{\xi}_\theta^c = \frac{3}{2\cdot(F+G+H)}\cdot\frac{\bar{\xi}_e^c}{\sigma_e}\cdot\sigma_z\cdot[2\cdot F+H]$$

$$\bar{\xi}_z^c = \frac{3}{2\cdot(F+G+H)}\cdot\frac{\bar{\xi}_e^c}{\sigma_e}\cdot\sigma_z\cdot[G-H] = \frac{3}{2\cdot(F+G+H)}\cdot\bar{\xi}_\theta^c\cdot\frac{G-H}{2\cdot F+H} \tag{16}$$

$$\bar{\xi}_r^c = -\frac{3}{2\cdot(F+G+H)}\cdot\frac{\bar{\xi}_e^c}{\sigma_e}\cdot\sigma_z\cdot[2\cdot F-G] = -\frac{3}{2\cdot(F+G+H)}\cdot\bar{\xi}_\theta^c\cdot\frac{G-H}{2\cdot F+H}$$

Intensity of the rates of logarithmic creep strains is expressed by the rate of logarithmic creep in circumferential direction:

$$\bar{\xi}_e = \frac{1}{HF+FG+GH}\cdot\sqrt{H\cdot(F\cdot\bar{\xi}_z - G\cdot\bar{\xi}_\theta)^2 + F\cdot(G\cdot\bar{\xi}_\theta - H\cdot\bar{\xi}_r)^2 + G\cdot(H\cdot\bar{\xi}_r - F\cdot\bar{\xi}_z)^2} \tag{17}$$

$$\bar{\xi}_e = \bar{\xi}_\theta\cdot\left[\frac{1}{HF+FG+GH}\cdot\sqrt{H\cdot\left(F\cdot\frac{G-H}{2\cdot F+H}-G\right)^2 + F\cdot\left(G+H\cdot\frac{2\cdot F+G}{2\cdot F+H}\right)^2 + G\cdot\left(-H\cdot\frac{2\cdot F+G}{2\cdot F+H} - F\cdot\frac{G-H}{2\cdot F+H}\right)^2}\right] \tag{18}$$

After a number of transformations the following expression for damageability parameter can be obtained

$$\omega = \int_0^t \frac{\sigma_e\cdot\bar{\xi}_e}{A_*}dt = \frac{\sqrt{4F+G+H}}{2F+H}\cdot\frac{\sigma_{e0}}{A_*}\cdot\int_1^x x\cdot dx = \frac{\sqrt{4F+G+H}}{2F+H}\cdot\frac{\sigma_{e0}}{2\cdot A_*}\cdot\left(x^2-1\right), \tag{19}$$

where dimensionless diameter of fuel rod cladding $x = \frac{D}{D_0}$.

From the condition that ω=1 under fuel rod cladding damage the specific scattering energy, accumulated by the moment of damage, can be determined which is a constant value and depends only on temperature.

$$A_* = \frac{\sqrt{4F+G+H}}{2F+H}\cdot\frac{\sigma_{e0}}{2}\cdot\left(x^2-1\right) \tag{20}$$

Specific scattering energy is associated with the energy of activation of creep mechanisms acting under the given temperature range. We use the dependence of specific scattering energy, accumulated by the moment of damage and obtained experimentally, on temperature in the form of

$$A_* = a_1\cdot\exp(-a_2\cdot T) \tag{21}$$

On taking the logarithm of expression (21) the linear dependence is obtained for approximation of specific scattering energy accumulated by the moment of damage:

$$y = b_0 + b_1\cdot x \tag{22}$$

where $y=\ln(A_*)$; $x=T$; $b_0=\ln(a_1)$; $b_1=-a_1$.

Parameters b_0 and b_1 were determined by the method of least squares:

$$b_0 = \frac{\sum_{i=1}^N x_i^2 \sum_{i=1}^N y_i - \sum_{i=1}^N x_i y_i \sum_{i=1}^N x_i}{N\cdot\sum_{i=1}^N x_i^2 - \sum_{i=1}^N x_i \sum_{i=1}^N x_i}; \qquad b_1 = \frac{N\cdot\sum_{i=1}^N x_i y_i - \sum_{i=1}^N x_i \sum_{i=1}^N y_i}{N\cdot\sum_{i=1}^N x_i^2 - \sum_{i=1}^N x_i \sum_{i=1}^N x_i} \tag{23}$$

where N- number of experimental points. Degree of connection between x and y was determined with the help of correlation factor.

$$r_{xy} = \frac{\frac{1}{N}\cdot\sum_{i=1}^N (x_i-\bar{x})\cdot(y_i-\bar{y})}{\sqrt{\sum_{i=1}^N (x_i-\bar{x})^2\cdot\sum_{i=1}^N (y_i-\bar{y})^2}} \tag{24}$$

At $r_{xy}=0$ the variables are not correlated, and at $r_{xy}=1$ they are linearly dependent of each other. Searching for the factors in system of equations (15) was made with the procedure of regression analysis.

Figs. 1,2 present plots of variation of relative circumferential strain for alloy Э110 at different internal pressures, obtained by calculations and experimentally with single specimens at constant internal pressure and different temperature, and in Figs. 3,4 the similar plots are presented for alloy Э635.

Alloy Э110

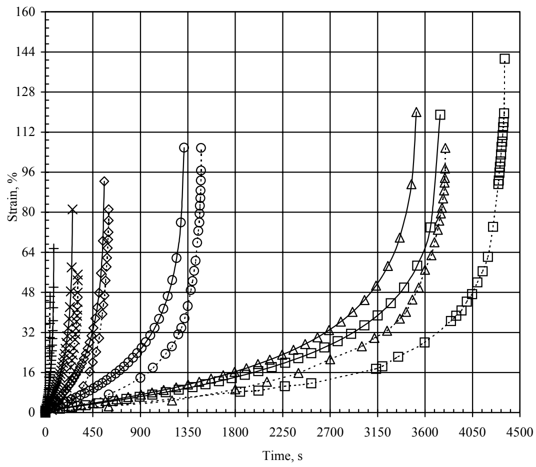


Fig.1. Maximum circumferential strain versus time.
(P= 2 MPa)

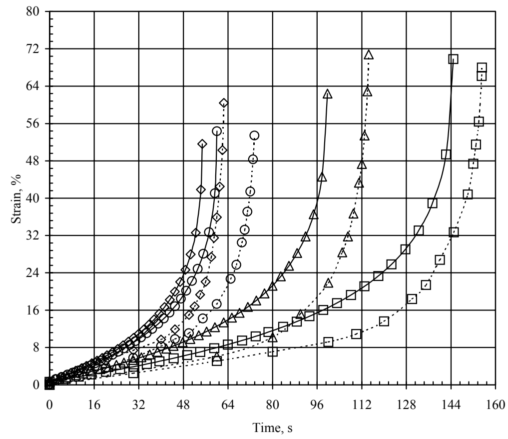


Fig.2. Maximum circumferential strain versus time.
(P= 12 MPa)

Alloy Э635

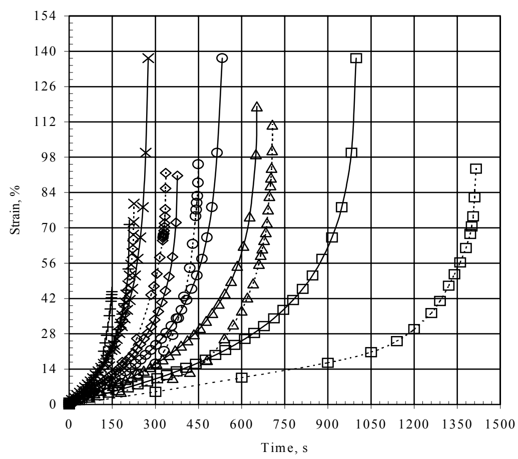


Fig.3. Maximum circumferential strain versus time.
(P= 2 MPa)

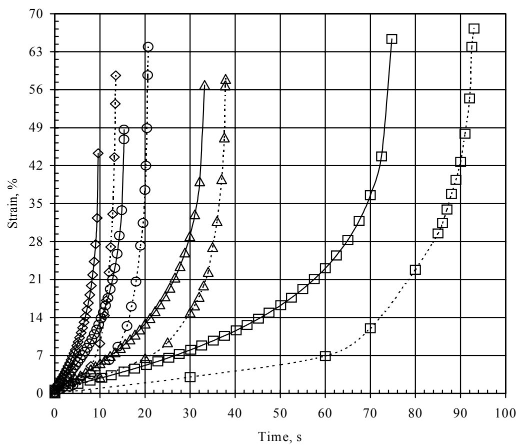
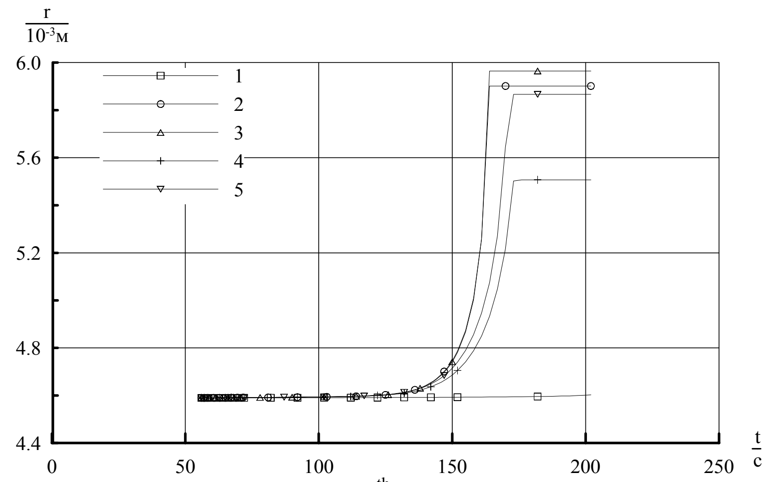


Fig.4. Maximum circumferential strain versus time.
(P= 12 MPa)

POST-TEST CALCUOLATION OF EXPERIMENTS AT THE BENCH PARAMETER-M

At the bench PARAMETER-M the tests were performed of the model assembly, comprising 37 simulators of fuel rods, under the conditions typical for scenario of LB LOCA. Thermophysical state in inter-fuel rod space and temperature of fuel rod claddings were determined from the solution of the associated problem with the use of thermohydraulic code with the module – code TVEL-3. Thermohydraulic code gives a possibility to perform calculation of time-variable pressure, temperature, density, flow rate and velocity of one-phase coolant and steam in the given case, and the conditions of heat transfer on the surface of fuel rod simulators.

Fig. 5 presents the calculated time variation of fuel rod clad radii of the model assembly and experimental points. Good agreement of calculated and experimental data confirm the correctness of the chosen approach to the analysis of fuel rods behaviour in the core for safety justification.



1 – central fuel rod 4 – 4th row fuel rods (group 3)
2 – 2nd row fuel rods 5– 4th row fuel rods (group 4)
3 – 3rd row fuel rods

Fig. 5 – Outer radius of the model FA clad surfaces

CONCLUSION

The model of the related solution of the problem of fuel rods behaviour in the reactor core under LB LOCA, presented in the paper, includes the model of fuel rod cladding ballooning by the mechanism of creep and damage which are described with kinetic equations. Forming the kinetic equations of creep and damage for alloys Э110 and Э635 was made with the use of tests of fuel rod cladding. Verification of models of straining and ballooning of fuel rod clads was made with tests of fuel rod claddings and model assemblies. With this, the test of model assemblies at the bench PARAMETER-M were used also for verification of the model of the associated solution of problems of fuel rod heat transfer with coolant, on the one hand, and of thermomechanical behaviour of fuel rod cladding, on the other hand.

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