Basic principles for lifetime and structural integrity assessment of BN-600 and BN-800 fast reactors components with regard for material degradation

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Abstract. The present paper overviews the basic principles of Russian Standard elaborated for justification of lifetime prolongation of BN-600 fast reactor (FR) and for justification of design lifetime of BN-800 FR. These principles are based on the analysis of the main mechanisms of material embrittlement and damage under service and formulation of the limit conditions for different components of FR of BN type.

Key Words: Fast reactor, Structural integrity, Embrittlement and damage mechanisms

1. Introduction

In 1980 a sodium cooled fast reactor BN-600 was commissioned, its operation life was designed to be 30 years. At the time of designing BN-600 reactor the methods of structural integrity and lifetime assessment for the main irreplaceable components were not sufficiently developed, therefore, the estimation was conservative to a great extent. This fact promoted the work package on the BN-600 reactor lifetime substantiation. This scope of activities was being conducted from 2004 to 2007 and was based on materials research carried out before and during the above period, analysis of the basic material fracture mechanisms during operation as well as on limit condition statements for different reactor components [1].

Based on the conducted materials investigations and developed procedures, in 2007 FSUE CRISM «Prometey» in cooperation with JSC «Afrikantov OKBM» worked out the standard document "Method for structural integrity assessment of fast neutron reactor components with sodium coolant" (RD EO 1.1.2.09.0714-2007) used for justification of BN-600 reactor lifetime extension up to 45 years. Later on, the above document was improved and its second version RD EO 1.1.2.09.0714-20011 was issued in 2011. The standard document was mainly improved by extending data on the physical and mechanical properties of materials taking into account their degradation induced by irradiation and thermal ageing.

At the same time as the BN-600 reactor lifetime extension activities JSC «Afrikantov OKBM» was involved in designing BN-800 and BN-1200 reactors. BN-600 and BN-800 reactors are close enough in their design, therefore for justification of BN-800 reactor structural integrity and lifetime the approaches used for BN-600 reactor could be principally used. At the same time, experience in BN-600 reactor operation allowed additional requirements to be introduced to structural integrity assessment not at the stage of lifetime extension but at the design stage. Based on the work package as part of designing BN-800 reactor, FSUE CRISM «Prometey» and JSC «Afrikantov OKBM» developed the standard document "Method for structural integrity assessment of fast neutron reactor components with sodium coolant at a design stage" (MT 1.2.3.06.0991-2014). The scope of this document was extended not only for BN-800 reactor under construction but also for BN-1200 reactor being designed. This expansion was quite justified as the neutron dose acting on the irreplaceable

BN-1200 reactor components is significantly lower than on the similar BN-800 reactor components at close operation temperatures.

The present paper is aimed at stating the basic considerations of Russian standards elaborated for justification of lifetime extension of fast BN-600 reactor and for justification of design lifetime of fast BN-800 reactor.

2. Operating conditions of BN type fast reactors and the main damage factors

Figure 1 exemplifies the structural design of BN-600 reactor. The RPV and internals are made of 18Cr-9Ni type steel (the Russian analogue of 304 steel). The main irreplaceable components of BN-800 and BN-1200 reactors are made of 18Cr-9Ni and 16Cr-11Ni-3Mo steel types (the Russian analogue of 316 steel). From data presented in Figure 1 it can be seen that the RPV of BN-600 reactor is exposed to insignificant neutron irradiation (the damage dose over 45 years does not exceed $1.2 \cdot 10^{-3}$ dpa) and its maximum operating temperature $T_{max}=450°C$ is rather low. RPV pressure is not high and makes up 0.14 MPa.

The neutron reflector material is exposed to the maximum neutron dose D (up to 43 dpa for 45 years) and operating temperature ($T_{max}=523°C$). Similar parameters for BN-800 reactor are up to D=30 dpa and $T_{max}=500°C$. Headers are also exposed to a high neutron dose (D≈5.5 dpa in BN-600 reactor).

Except for a high neutron dose and elevated temperatures the material of BN-600 and BN-800 reactors is subjected to low-cycle thermal loading due to coming up periodically to full capacity after scheduled or emergency shutdowns.

Besides, some reactor components in the region of mixing «cold» and «hot» sodium are subjected to high-cycle thermal loading due to the alternating contact of these components with «cold» and «hot» sodium. The frequency of changing the contact from «cold» to «hot» sodium can vary from 0.1 to 10 Hz. The spread of «cold» and «hot» sodium temperatures can achieve 30÷55°C at a 0.1 Hz frequency of thermopulsations.

The described process was called as the high-cycle loading of a material due to the coolant thermopulsations.

3. The main embrittlement and damage mechanisms of BN-type reactor materials

All the main embrittlement and damage mechanisms of materials of BN-type reactor materials can be divided into three groups.

Group I includes material degradation (embrittlement) mechanisms under neutron irradiation.

Group II includes material degradation mechanisms due to thermal ageing.

Group III includes material degradation mechanisms due to thermal and mechanical action. For BN-type reactor components such mechanisms are considered to be creep and fatigue damage.

It should be noted that the mechanisms of groups I and II can mutually enhance the material embrittlemen. Besides, material degradation mechanisms of groups I and II can enhance the material damage induced by thermal and mechanical actions.
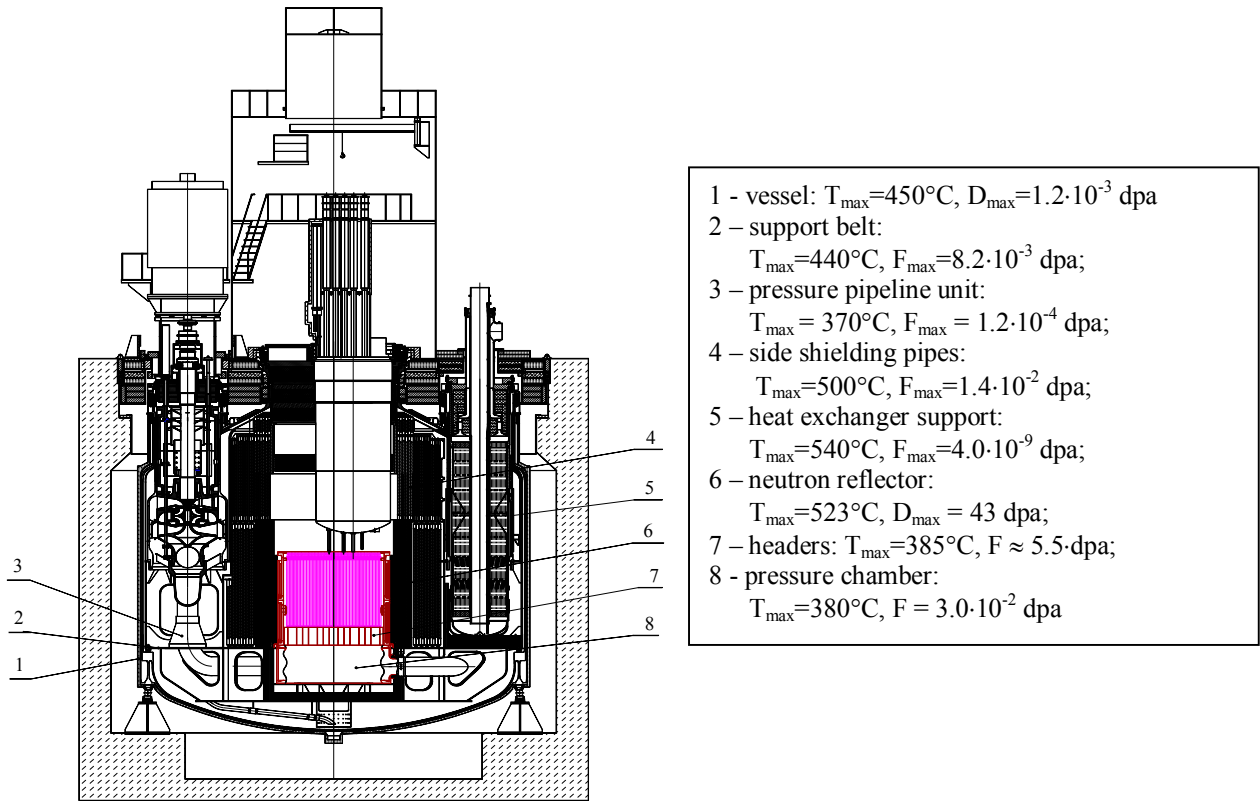
1 - vessel: $T_{max}=450°C$, $D_{max}=1.2·10^{-3}$ dpa
2 – support belt:
   $T_{max}=440°C$, $F_{max}=8.2·10^{-3}$ dpa;
3 – pressure pipeline unit:
   $T_{max} = 370°C$, $F_{max} = 1.2·10^{-4}$ dpa;
4 – side shielding pipes:
   $T_{max}=500°C$, $F_{max}=1.4·10^{-2}$ dpa;
5 – heat exchanger support:
   $T_{max}=540°C$, $F_{max}=4.0·10^{-9}$ dpa;
6 – neutron reflector:
   $T_{max}=523°C$, $D_{max} = 43$ dpa;
7 – headers: $T_{max}=385°C$, $F ≈ 5.5·dpa$;
8 - pressure chamber:
   $T_{max}=380°C$, $F = 3.0·10^{-2}$ dpa

FIG. 1. Fast sodium reactor BN-600

In spite of the fact that all the irreplaceable components of BN reactor plant are made of ductile austenitic chromium-nickel steels, high-dose neutron irradiation leads to embrittlement of these steels.

Embrittlement of austenitic steels under neutron irradiation generally occurs by the following mechanisms.

3.1. Material embrittlement mechanisms under neutron irradiation

Mechanism Em_{H-S}. This mechanism is caused by two processes occurring under neutron irradiation: material hardening and radiation-induced segregations of alloying and impurity elements. Material hardening and segregation of impurity elements (in particular, phosphorus) at the phase boundaries (inclusion-matrix interphase) makes the nucleation of voids on inclusions easier [2]. Besides, an increase in yield stress under neutron irradiation is accompanied by a decrease in the material strain hardening [3, 4]. The above processes lead to a decrease in the material ductility and fracture toughness by the ductile fracture mechanism [5, 6].

The design neutron dose dependence of fracture strain ε_f and fracture toughness J_c for austenitic steels and their welds embrittled by the mechanism Em_{H-S} is described by the following equation [5]:

$$\varepsilon_f = \varepsilon_f^0 \cdot \left[1 - A_\varepsilon \cdot \sqrt{1 - \exp(-B_\varepsilon \cdot D)}\right], \qquad (1)$$

$$J_c = C_J \cdot \frac{\sigma_y(D,T) + \sigma_{ul}(D,T)}{2} \cdot \left[0.1 + 0.9 \cdot \exp(-0.3 \cdot D)\right]. \qquad (2)$$

In Eq.(1) ε_f^0 is fracture strain for material in initial unirradiated condition; A_ε and B_ε are material constant, for 18Cr-10Ni steel A_ε=0.53, $B_\varepsilon = 0.117$ dpa^{-1}.

In Eq.(2) σ_y and σ_{ul} are yield and ultimate strength correspondingly; C_J is material constant, for 18Cr-9Ni grade steel $C_j = 0.27$ and for weld metal $C_j = 0.19$.

Mechanism Em$_{Sw}$. This mechanism is due to the effect of radiation swelling on the material fracture strain and fracture toughness. When a material is subjected to deformation vacancy voids grow along with deformation voids nucleated on inclusions. Therefore, with an increase in swelling, i.e. with an increase in the concentration and diameter of vacancy voids, the material ductility and fracture toughness decrease. [6] It should be noted that swelling has a much more considerable effect on the material fracture toughness than on its fracture strain. It is related to a sharp decrease in the process zone, i.e. the zone where an elementary act of material fracture occurs. If there is no swelling, the process zone size correlates with the distance between inclusions where the nucleation of deformation voids takes place. With swelling S≥5 % the process zone size correlates with the distance between vacancy voids [6].

Design dependences predicting the effect of swelling on the fracture strain $\varepsilon_f(S)$ and fracture toughness $J_c(S)$ are presented by following equations [6]

$$\varepsilon_f(S) = \varepsilon_f^{Em_{H-S}} \cdot \varphi(S), \tag{3}$$

$$J_c(S) = J_c^{Em_{H-S}} \cdot \varphi_J(S) \cdot \left[1 - \left(\frac{S}{1+S} \right)^{2/3} \right], \tag{4}$$

where $\varepsilon_f^{Em_{H-S}}$ and $J_c^{Em_{H-S}}$ are fracture strain and fracture toughness correspondingly for material embrittled by ***Em$_{H-S}$*** mechanism; S is radiation swelling; $\varphi(S)$ and $\varphi_J(S)$ are the following functions describing effect of swelling

$$\varphi(S) = \exp\left(-6.55 \cdot S^{0.67}\right) \tag{5}$$

$$\varphi_J(S) = \left(1 - 19.03 \cdot S\right) \cdot \exp\left(-6.94 \cdot S^{0.865}\right) \tag{6}$$

Mechanism Em$_{tr}$. This mechanism is due to radiation-induced nickel segregation occurring under neutron irradiation [7, 8]. Nickel segregates on the grain boundaries as well as on the free surfaces of a material which are the surfaces of vacancy voids [7].

With an increase in swelling the total area of the free surface of vacancy voids related to the unit volume of material matrix increases. Therefore, the segregation of nickel near to vacancy voids grows and the depletion of nickel in the material matrix outside of voids occurs. With a certain level of nickel depletion in the material matrix, a partial $Fe_\gamma \rightarrow Fe_\alpha$ phase transformation happens. Due to the $Fe_\gamma \rightarrow Fe_\alpha$ transformation, the ductile-to-brittle transition typical for bcc-lattice materials becomes possible. Transition from the ductile to brittle condition is accompanied by a sharp decrease in the material ductility to zero [8]. Since the described embrittlement mechanism is controlled by radiation swelling, the realization of Fe $\gamma \rightarrow$ Fe α transformation where the ductile-to-brittle transition occurs can be uniquely related to the level of radiation swelling [8]. For steels of 18Cr - (9÷10) Ni type the critical value of swelling S_{crit} where the ductile-to-brittle transition occurs is ≈7 % [8].

Mechanism Em$_{He}$. This is a mechanism of so-called high-temperature radiation embrittlement caused by a decrease in the strength of grain boundaries due to higher helium pressure with an increase in temperature. Helium is generated from nuclear reactions through the interaction of neutrons with such elements as Ni, B and Fe. The mechanism Em$_{He}$ leads to a sharp decrease

in fracture strain and fracture toughness at temperatures above ≈500°C. Fracture by this mechanism is intergranular.

When constructing design dependences of the material embrittlement by this mechanism we used the following statements based on experimental data.

a) The fracture strain ε_f practically does not depend on irradiation temperature and decreases sharply with an increase in test temperature, especially at $T_{test} > 500°C$.

b) Since thermal ageing happens at the same temperatures as the mechanism Em_{He}, a decrease in ε_f with an increase in T_{test} due to both mechanisms can be described by the unified function.

c) The dependence ε_f when the mechanism Em_{He} realizes can be presented as

$$\varepsilon_f^{Em_{He}} = \varepsilon_f^{Em_{H-S}} \cdot \Omega(T_{test} - T_{He}), \qquad (7)$$

where $\Omega(T_{test}-T_{He})$ is a function describing the decrease of fracture strain on test temperature by Em_{He} mechanism; T_{He} is the minimal temperature when mechanism Em_{He} is realized.

d) Experiments carried out on smooth and notched specimens have shown that in case of fracture by the mechanism Em_{He} the value of ε_f does not depend on stress triaxiality.

Therefore, fracture toughness can be calculated from the following dependence

$$J_c^{Em_{He}} = J_c^{Em_{H-S}} \cdot \Omega(T_{test} - T_{He}). \qquad (8)$$

In Eq. (8) the function $\Omega(T_{test}-T_{He})$ is identical to that used in the Eq. (7).

Based on the above statements and obtained experimental data we proposed the following function $\Omega(T_{test}-T_{He})$

$$\Omega(T_{test} - T_{He}) = 1 - (2.25 \cdot 10^{-5} \cdot T_{He} - 5.34 \cdot 10^{-3}) \cdot (T_{test} - T_{He}), \qquad (9)$$

where value of T_{He} for conservative estimation of fracture strain and fracture toughness over the range of operation temperatures for the BN-type reactor is taken as 400°C.

The material embrittlement mechanisms Em_{H-S}, Em_S and Em_{He} proceed simultaneously, therefore the real values of ε_f and J_c at some neutron irradiation parameters (D, T_{irr}) and test parameters (T_{test}) will be determined as follows:

$$\varepsilon_f = \min\{\varepsilon_f^{IG}, \varepsilon_f^{TG}\}, \qquad (10)$$

$$J_c = \min\{J_c^{IG}, J_c^{TG}\}, \qquad (11)$$

where ε_f^{IG} and J_c^{IG} are fracture strain and fracture toughness for material embrittled by Em_{He} mechanism when fracture is intergranular, in fact $\varepsilon_f^{IG} = \varepsilon_f^{Em_{He}}$ and $J_c^{IG} = J_c^{Em_{He}}$; ε_f^{TG} and J_c^{TG} are fracture strain and fracture toughness for material embrittled by Em_{H-S}, Em_S mechanisms and thermal aging mechanism (see section 3.2) when fracture is transgranular.

Radiation swelling and radiation creep. Except for its effect on the material embrittlement, radiation swelling can have a direct effect on the structural integrity and serviceability of the reactor components.

The dependence of free radiation swelling S_0 on temperature and neutron dose for 18Cr-9Ni steel is described as follows [10]

$$S_0 = c \cdot \left(\frac{D}{D_0}\right)^n \cdot \exp\left(-r \cdot (T_{irr} - T_m)^2\right) \tag{12}$$

where $c = 1.63 \cdot 10^{-4}$, $n = 1.88$, $D_0 = 1$ dpa, $r = 1.1 \cdot 10^{-4}$ C^{-2}, $T = 470°C$.

Different zones of the reactor components are exposed to different neutron doses at different temperatures. Such a situation leads to a radiation swelling gradient, and consequently to the arising additional stresses in the reactor components and a decrease of its structural integrity.

Besides, radiation swelling results in the dimensional change of the reactor components that in some cases can lead to the abnormal reactor operation, for example, because of the wedging of its mobile components.

Radiation creep, which depends on neutron dose and swelling, leads to a decrease in stresses caused by swelling. Therefore, in order to avoid a too conservative estimation of structural integrity and serviceability of the reactor components it is necessary to make calculations taking into account both radiation swelling and radiation creep.

When calculating radiation swelling it is necessary to take into account the effect of stresses on the swelling rate [11].

When making calculations of the stress-strain state of the reactor components the following deduced equations are used [10, 11]

$$\dot{S} = \dot{S}_0 \cdot \left[1 + P \cdot \left((1 - \eta) \cdot \sigma_m + \eta \cdot \sigma_{eq}\right)\right], \tag{13}$$

$$\dot{\varepsilon}_{eq}^c = \left(B \cdot \frac{dD}{dt} + \omega \cdot \dot{S}\right) \cdot \sigma_{eq}, \tag{14}$$

where $\dot{S}$ is swelling rate taking into account effect of stresses, $\dot{\varepsilon}_{eq}^c$ is creep strain rate, σ_m is hydrostatic component of stress; σ_{eq} is equivalent stresses; P, η, B and ω are material constant, for 18Cr-9Ni grade steel $P = 8.0 \cdot 10^{-3}$ MPa^{-1}, $\eta = 0,15$, $B = 1 \cdot 10^{-6}$ $(MPa \cdot dpa)^{-1}$; $\omega = 2.7 \cdot 10^{-3}$ $(MPa)^{-1}$.

3.2 Thermal material ageing mechanisms

The conducted investigations have shown that at temperatures of 450-550°'C the thermal ageing of austenitic steels generally occurs due to the precipitation of $Me_{23}C_6$ carbides [12]. These carbides precipitate both within the grain and at the grain boundaries. Carbide precipitation leads to a decrease in ductility, impact toughness and fracture toughness for austenitic steels.

It should be noted that thermal ageing leads to a considerable material embrittlement enhancement by the mechanism Em_{He}.

3.3 Material damage caused by thermal and mechanical action

3.3.1 Creep fracture under static loading

Fracture of austenitic steels for a long period of time usually proceeds by the intergranular mechanism. Such fracture is due to the nucleation and growth of lens-shaped (crack-like) voids on the grain boundaries [13] The nucleation rate of grain-boundary voids increases with increase of intergranular sliding. The material hardening under neutron irradiation is generally caused by an increase in resistance to the grain body deformation. Hence, the portion of

intergranular sliding increases with increasing a neutron dose. Therefore, neutron irradiation increases the nucleation rate of grain-boundary voids.

The growth of grain-boundary voids is caused by plastic deformation and diffusion of vacancies. Neutron irradiation increases the diffusion of vacancies and thus increases the voids growth rate. Moreover, neutron irradiation leads to increase of creep rate. That in turn, leads to increase of nucleation rate and growth rate of voids. Thus, neutron irradiation reduces the time to rupture of material under creep [14].

3.3.2 Low-cycle fatigue fracture under cyclic loading

Low-cycle fatigue can be conventionally divided by mechanisms of material deformation. At $T \leq 450°C$ deformation generally occurs due to instant plastic strain without creep. At $T > 450°C$ deformation occurs both due to instant plastic strain and creep. In this case, the material fatigue life is determined not only by the strain range but also by the time factor – the strain rate. At $T \leq 450°C$ the time factor can be neglected. The effect of neutron irradiation on fatigue resistance can be taken into account through the consideration of the effect of neutron irradiation on the standard tensile properties or fracture properties obtained in creep tests. The data on the material creep-rupture strength and ductility under irradiation can be used for constructing fatigue curves at $T > 450°C$ [15].

3.3.3. Fracture under high-cycle loading is caused by thermopulses. The number of thermopulses for the reactor operation end of life can exceed 1×10^{10}. It is obvious that the reliable fatigue limit estimation with such a number of cycles cannot be made properly without taking into account the real surface roughness of the reactor components as well as the scale factor. Under thermopulses the cyclic stresses are maximum on the surface and decrease in the direction of the component thickness. For such cyclic stress distribution the situation is quite possible when an initiated surface fatigue crack will be arrested with its insignificant propagation into the component. The analysis of the crack arrest can be made with the determined value of the threshold stress intensity factor range ΔK_{th}.

Thus, the fracture analysis under high-cycle loading can be made by the criterion of fatigue crack arrest [16]. Neutron irradiation leads to an increase in the material yield strength. Taking into account that σ_{-1} usually correlates with $\sigma_{0.2}$, and ΔK_{th} correlates with σ_{-1}, a conclusion can be made that neutron irradiation, at least, does not lead to a decrease in ΔK_{th}. Hence, for providing the conservative estimation ΔK_{th} can be determined on an unirradiated material.

4. Statement of structural integrity and serviceability conditions for the reactor components

4.1. Critical events

Based on the analysis of embrittlement mechanisms and material damage, the following critical events were introduced. The combination of these events can lead to loss of structural integrity or serviceability of the reactor components. A critical event is understood as the component condition when one of following events takes place in some component zone:

Event 1 – Crack nucleation under cyclic loading by the fatigue mechanism (the components are considered for which creep can be neglected at the maximum operating temperature).

Event 2 – Crack nucleation under long-term cyclic or long-term static loading (the interaction of creep and fatigue).

Event 3 – Crack nucleation under ratcheting caused by thermal and mechanical loading. This mechanism can be realized for the reactor components only under creep, since according to the standard documents nominal stresses due to mechanical loading should be lower than the material yield strength.

Event 4 – Formation of a limit embrittlement area (LEA) due to the partial $Fe_\gamma \rightarrow Fe_\alpha$ phase transformation. Since ductility in this zone is nearly equal to zero, a crack in this zone can be nucleated at any moment of the reactor operation. Therefore, for providing the conservative estimation it is reasonable to accept that there is a crack in the component with the size equal to the size of the LEA.

Event 5 – Unstable crack propagation. Neutron irradiation combined with radiation swelling can lead to a very significant decrease of fracture toughness. With a low level of material fracture toughness, fast ductile fracture in component is possible directly after the crack start.

Event 6 – Loss of carrying capacity of the reactor component.

Event 7 – - Loss of the component tightness. This critical event is considered only for the components holding pressure.

Event 8 – Inadmissible change of geometrical sizes of a component. This critical event can be realized due to radiation swelling and radiation creep or due to ratcheting. The change of the geometrical size of a component or a group of components can lead to their disfunction, for example, to wedging, etc.

Event 9 – Crack nucleation by the ductile fracture mechanism. This critical event can arise only under specific reactor loading conditions, namely, in case of design accidents and external dynamic actions (for example, in case of earthquake). For the above conditions, it is not required to provide the reactor functional serviceability but it is necessary to provide such safety conditions as the reactor vessel tightness, automatic accident shutdown of reactor as well as the possibility of post-accident discharging of the reactor core. Therefore, for the above conditions some plastic strain is allowed, which generally can lead to the crack initiation by the ductile fracture mechanism.

The above-mentioned conditions and the critical event 9 are not analyzed in this paper.

4.2. Structural integrity and lifetime of main components of reactor

Many reactor components include welds. It well known that in manufacturing of a component the probability of defect formation in the weld is much higher than in the base metal. Hence, a defect can appear before the component operation. In-service inspection of defects in the reactor component welds is practically impossible. Therefore, for the conservative estimation of reactor structural integrity it is assumed that all the welds have surface cracks whose depth is equal to the pass height. Except for the welds, defects can be formed in a special cladding. Some components have this cladding for providing wear resistance in the components sliding between each other. The cladding is very brittle. Therefore, the formation of a crack with depth equal to the cladding thickness cannot be excluded. Therefore, for the conservative estimation of reactor structural integrity it is assumed that all the components with special cladding have a surface crack whose depth exceeds the cladding thickness.

Let's define which components should be referred to as the main reactor components. The following definition of the main components was given: the main components are components for which change of geometrical sizes, damage or fracture can disturb the normal (design) reactor operation as a whole.

Depending on the severity of damage and fracture effects of one or another component, all the main components were subdivided into two groups.

Group A includes the main components whose principal function is to hold pressure.

Group B includes the main components whose functions do not include pressure holding.

The structural integrity of Group A components is provided, if the following conditions are met. Critical events 1, 2, 3 and 4 are not realized over the considered operation period. The analysis of the above critical events is made with the assumption that there are no defects in the component under consideration. Critical events 5 and 7 are not realized for the entire operation period with the assumption that there is a postulated defect the component.

In case when critical event 1 is realized for the component subjected to thermopulses ($N>1\times10^6$ cycles), the structural integrity of such a component is considered to be provided if the additional analysis was made and the following was shown: the relative depth of a crack a/w does not exceed 0.25 and critical event 5 is not realized for the considered operation period.

From the above structural integrity condition, the Group A component lifetime is determined by the formula

$$t_{life} = \min\left\{t_f^{nucl}, t_f^{prop}\right\} \tag{15}$$

where t_f^{nucl} is the time before the crack initiation or formation of LEA according to critical events 1, 2, 3 and 4; t_f^{prop} is the time of crack propagation from the postulated defect to the critical size determined by critical events 5 and 7.

Thus, the structural integrity and lifetime of Group A components are provided "twice": both by the criterion of crack initiation and by the criterion of crack propagation.

The formulation of structural strength condition for Group B components is less conservative. The structural integrity of Group B components is provided, if critical events 5 and 6 are not realized for all the considered operation period. Therewith, it is assumed that critical events 1, 2, 3 and 4 do not lead to the loss of structural integrity of the considered component. For the component having the welds or the component with special cladding it is assumed that such components have a postulated defect (a surface crack) before the start of reactor operation. This defect propagates during operation. The lifetime for such components is calculated by the formula

$$t_{life} = t_f^{prop} \tag{16}$$

where t_f^{prop} is the time of crack propagation from the initial size to the critical size determined by critical events 5 and 6.

For the components with no welds and the cladding it is assumed that there are no defects and at first the analysis of critical events 1, 2, 3 and 4 is made. In case where no one of these events is not realized for the considered operation period, the calculation of critical events 5 and 6 is made for a postulated defect equal to the initiated crack size. For such components the lifetime is calculated by the formula

$$t_{life} = t_f^{nucl} + t_f^{prop}. \tag{17}$$

The serviceability of a component is considered to be provided when for the considered operation period the condition of structural integrity is provided and critical event 8 is not realized.

Conclusions

1. The general statements are stated for standard documents for the assessment of structural integrity and lifetime of the main components of BN type fast sodium-cooled reactors.

2. It is shown that the developed standard documents take into consideration all the potential mechanisms of degradation and material damage typical for operating conditions of the BN type reactors.

3. A brief overview of different embrittlement mechanisms of a material under neutron irradiation and at higher temperatures is presented.

4. Possible approaches to the calculations of material damage and structural integrity are shown under thermal and mechanical action (creep, fatigue) taking into account material embrittlement induced by neutron irradiation and thermal ageing.

5. The peculiarities of the assessment of structural integrity and lifetime of the reactor components are considered.

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