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Design Considerations for Dissimilar Material Modeling of RPV Lower Head Creep under Severe Accident Conditions

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Abstract

The lower head of the reactor pressure vessel (RPV) can be subjected to significant thermal and pressure loads in the event of a core meltdown accident. For the detailed understanding of its behavior, a real scale experiment of RPV creep with prototype material, namely low alloy steel, is in demand. But it is highly difficult to perform because of very high heat flux and pressure. If we can replace the real test of the prototype material using dimensional analysis with a model material that possesses constitutive similarity but has low melting temperature and mechanical strength, the experiment can be significantly simplified and less expensive.

From the mathematical structure of the constitutive equation for classical viscoplasticity, a simple rheological model was derived. The model explains the time dependent mechanical behavior of RPV creep. The creep equation was nondimensionalized using the dimensionless group of variables. By adopting lead (Pb) as a model material, heat flux and pressure conditions of the model experiment was defined.

Finite element analyses showed adequate agreement between prototype and model systems for the time dependent deformation behavior on nondimensional coordinates such that this novel approach can be used under the scaled temperature/pressure conditions to represent creep deformation behavior of prototype RPV.

1. Introduction

In the event of a core meltdown accident, the possibility exists that the lower head will fail and release large amounts of the molten core material to the containment floor [1]. The mode, timing, and size of lower head failure are of prime importance in the assessment of core melt accidents because they define the initial conditions for ex-vessel events such as core/basemat interactions, fuel/coolant interactions, and direct containment heating. On the other hand, some studies indicate that the deformation of the lower head wall may lead to the inner wall gap cooling conductive to in-vessel core retention without external cooling [2,3]. However the current understanding is that in-vessel retention approach by external cooling will be successful only when the lower head deformation can be conservatively limited.

There are two possible failure mechanisms for the RPV lower head under severe accident. If the RCS pressure is negligible, the failure mechanism will be simple collapse by lower head melting. Although most reactors adopt safety depressurization systems for use in severe accidents, it is reasonable to assume there will be certain level of residual pressure even under the depressurization conditions. In those cases, the lower head failure mechanism will be creep rupture. Creep deformation of the lower head vessel will eventually lead to creep rupture, as is illustrated in the Lower Head Failure (LHF) experiments at Sandia

National Laboratories [4]. From the LHF experiments and the in-vessel retention point of view, it can be concluded that creep deformation of the vessel is of prime importance in that

a) lower head creep can lead to global vessel rupture,
b) creep deformation of the lower head can lead to premature failure of penetration nozzles, and
c) creep deformation affects the ex-vessel and in-vessel processes by interfering with other surrounding media and structures.

Real scale experiment of RPV creep with prototype material, namely low alloy steel, is in demand to understand its behavior but highly difficult to perform because such experiment requires fairly high heat flux and higher pressure facilities. In addition, in order to perform an ex-vessel cooling experiment it is necessary to set up a protection system against creep rupture and/or steam explosion, etc. The experiment at a full scale becomes highly expensive and is clearly impractical if not impossible. If we can replace the real test of the prototype material using the dimensional analysis with a model material that possesses constitutive similarity but has low melting temperature and mechanical strength, the experiment can be significantly simplified and less expensive. The purpose of this paper is to establish a dimensional analysis for creep behavior of a reactor-scale vessel to bring in a design procedure for a small-scale model experiment of different material directly relevant to the prototype reactor. The key features of the problem can be seen with the help of Fig. 1, depicting the reactor pressure vessel with its boundary conditions of environmental temperature T_∞ and heat transfer coefficient to the environment h, the values of which are listed in Table 1. We would employ the term “dissimilar material modeling” in a special context in this study. The term will connote modeling of prototype structures with models that are made of materials differing from those in the prototype, but possessing constitutive similarity [5].

This paper consists of four technical sections. Two sections (Section 3 and Section 4) address the mathematical modeling and nondimensionalization procedures, respectively. Section 5 addresses the defining scheme for the loading conditions of the model experiment and Section 6 is devoted to the analysis results and discussion. The overall aim of these sections and their interrelations are explained as a part of the overall approach and methodology in Section 2. The paper comes to an end with the conclusions in Section 7.

2. Problem Definition and Overall Approach

Two different systems of RPV lower head with different size and material of constituent have been considered. For the derivations of scaling rule, we assume one-dimensional spherical symmetry [6]. The prototype system is assumed as a hemispherical shell of low alloy steel with inner radius of 2.371 m and outer radius of 2.536 m, after the dimensions for the case of Korean Next Generation Reactor (KNGR). The scaled model system is assumed to be made of “model material” with the scale of 1/34 the prototype systems. All dimensions including material parameters with their values for both the prototype and the model systems used in the calculations are listed in Table 1.

Physical parameters that are less important are eliminated from our dimensional analysis after having practiced in selecting important physical parameters [7]. Mathematical relationships were found using the variables that govern creep behavior. In designing scale experiments, it is very important to select the nondimensional parameters correctly. There should be as few parameters as possible, while they still can reflect fundamental effects in the most convenient way.

A basic assumption in the creep formulation for the system is that the total strain can be expressed as the sum of elastic and creep strains. As the model, a hemispherical shell of 15 cm O.D. and 5 mm wall made of lead is adopted. In this study, to be used as preliminary design information before a test, heat flux and pressure conditions for the model system are calculated using a dimensional analysis of the creep equation and mathematical model as a guide.

As a demonstration of correctness of the scaling procedure, finite element analysis was performed for both prototype and model conditions. In the analysis, thermal expansion is additionally considered which is not directly included as a scaling parameter but has an effect to deformation, when creep strain is not dominant, especially in a prototype system where temperature increase is much larger.

3. Mathematical Modeling

In this section we begin with the mathematical structure of the constitutive equations for classical viscoplasticity by a simple rheological model [8]. Our objective is to reinstate in the simplest possible context the formulation of the general viscoplastic models. It consists of a spring with the elastic constant of E, and a dashpot with viscoplasticity η, in series as shown in Fig. 2. The model explains the time dependent deformation behavior of RPV in a circumferential direction.

Under the action of an overall stress σ there will be an overall strain ε in the system which is given by

$$\varepsilon = \varepsilon_1 + \varepsilon_2 \tag{1}$$

where ε_1 and ε_2 are the strains in the spring and dashpot, respectively.

Since the elements are in series the stress is obviously the same, as follows

$$\sigma_1 = \sigma_2 = \sigma \tag{2}$$

Equations (1) and (2) can be written therefore as for the spring and dashpot respectively, by the linear elastic behavior given by Hooke's law and the linear viscous behavior given by Newton's law [8].

$$\frac{\partial \sigma}{\partial t} = E\frac{\partial \varepsilon_1}{\partial t}, \quad \sigma = \eta\frac{\partial \varepsilon_2}{\partial t} \tag{3}$$

In this study the viscosity η is assumed to be a function of stress σ, temperature T, and, time t, namely $\eta = \eta(\sigma, T, t)$, to describe typical creep behavior. Differentiation of Eq. (1) using Equation (3) gives

$$\frac{\partial \varepsilon}{\partial t} = \frac{1}{E}\frac{\partial \sigma}{\partial t} + \frac{\sigma}{\eta(\sigma, T, t)} \tag{4}$$

In case of creep of our problem the stress can be thought as held almost constant at $\sigma \cong \sigma_0 (= E\varepsilon_0)$ and so $d\sigma / dt \cong 0$ where the definition of σ_0 and ε_0 will be described in next section. Equation (4) therefore be simply written as by introducing the creep function, $f(\sigma, T, t)$

$$f(\sigma, T, t) = \frac{\partial \varepsilon}{\partial t} = \frac{\sigma}{\eta(\sigma, T, t)} \tag{5}$$

Now the total strain at time t, is denoted by the single equation

$$\varepsilon = \varepsilon_0 + \int_0^t f(\sigma, T, t)dt \tag{6}$$

The total strain is composed of two parts as shown in Eq. (6). To obtain the similarity of total strain, our approach is to choose both ε_0 and $f(\sigma, T, t)$ as major scaling parameters.

4. Nondimensionalization of Creep Equation

To simplify creep equation, the Bailey-Norton-type equation with Arrehnius-type temperature dependency for the secondary creep is defined as follows [9]

$$f(\sigma, T, t) = \frac{\partial \varepsilon}{\partial t} = A\sigma^m \exp\left(-\frac{Q}{RT}\right) \tag{7}$$

where $\dot{\varepsilon}, A, m, Q, R, \text{and } T$ are creep strain rate, Bailey-Norton coefficient, Bailey-Norton exponent, thermal activation energy, the universal gas constant, and temperature, respectively. These creep constants are listed in Table 2.

To nondimensionalize Eq. (7), we select the following parameters and each parameter is defined as dimensionless strain λ, dimensionless stress Σ, dimensionless temperature Θ, and dimensionless time τ, respectively.

$$\lambda = \varepsilon / \varepsilon_0, \Sigma = \sigma / \sigma_0, \Theta = \frac{Q}{R}\left(\frac{1}{T} - \frac{1}{T_0}\right), \tau = AE\sigma_0^{m-1} t \exp\left(-\frac{Q}{RT_0}\right) \tag{8}$$

where T_0 is the reference temperature at which both model and prototype materials have the same dimensionless time measure and σ_0 is a membrane stress and ε_0 is the corresponding elastic strain respectively, as defined below:

$$\sigma_0 (= E\varepsilon_0) = \frac{Pr}{2(b-a)} = \text{membrane stress of spherical lower head} \tag{9}$$

Substitution of Eq. (8) into Eq. (7) yields the following dimensionless creep equation

$$\frac{\partial \lambda}{\partial \tau} = \dot{\lambda} = \Sigma^m e^{-\Theta} \tag{10}$$

The procedure for the dimensional analysis is as follows. First, we determine the reference membrane stress and corresponding reference elastic strain of the prototype material using Eq. (9). Then by equating the elastic strain of the model system with prototype system, the reference stress and pressure of the model system is determined. Equating the dimensionless time τ, obtain the reference temperature T_0. It is noted that if two bodies of like geometry have the same stress exponent m, dimensionless stress Σ, and dimensionless temperature Θ, they reach the same dimensionless creep strain λ, at the dimensionless time τ. However, the stress exponent m, is a material property and generally differs from material to material. On the other hand, it is derivable to set the reference stress σ_0, as the membrane stress, such that the value of dimensionless stress Σ, becomes about unity, making dimensionless strain λ, not sensitive to the stress exponent m.

5. Defining Loading Conditions

For the prototype condition, internal pressure of 2 MPa is assumed as a conservative value of safety depressurization system and heat flux of 418 kW/m^2 is prescribed as a typical value in case of severe accident of KNGR [7]. The temperature of the environment is assumed to be 418 K and a heat transfer coefficient to the environment of 30 W/m^2K is chosen [10]. If we are to use the strain as the primary scaling parameter, elastic strains of both prototype and model are to be equated. By equating the elastic strain of the model with that of the prototype, internal pressure of the model is uniquely determined by the help of Eq. (9) and using the elastic modulus value of Table 1.

The next step is to define the heat flux of the model experiment. According to the high heat flux of the prototype condition, there will be large temperature differentials across the vessel. Before scale the heat flux for the model system, it must be noted that what we are scaling is not the temperature differentials but the creep deformation. The model experiment designed here is a small-scale experiment and temperature differentials across the vessel are almost negligible. So the most representing single temperature must be picked from the temperature differentials across the vessel to be scaled at the model system.

If it is assumed either that radial stress is almost little responsible for the creep or that radial stress is negligible compared to the circumferential stress, then the stress of the circumferential direction integrated across the vessel can be set as equivalent to value of the membrane stress multiplied with its corresponding area.

$$\pi(b^2 - a^2)\sigma_0 = \int_a^b (2\pi r)\sigma(r)dr \tag{11}$$

The creep strain rate of various temperatures of Eq. (7) can be approximated as that of equivalent temperature and membrane stress, thus

$$A\sigma^m \exp\left(-\frac{Q}{RT}\right) = A\sigma_0{}^m \exp\left(-\frac{Q}{R\tilde{T}}\right) \tag{12}$$

where $\tilde{T}$ is the effective temperature

These assumptions and derivations have been described in full in reference [7], where the complete formation for the cases is given. Finally, rearranging the above Eqs. (11-12), effective temperature $\tilde{T}$ is obtained

$$\tilde{T} = \frac{Q/R}{m \ln\left\{\int_a^b \frac{2r}{b^2 - a^2} \exp\left(\frac{Q}{mRT}\right) dr\right\}} \tag{13}$$

Fig. 2 shows the temperature histories across the vessel as function of time with their effective temperature calculated. Effective temperature then was used to calculate the dimensionless temperature as function of dimensionless time as depicted in Fig. 3 using Eq. (8).

Nondimensionalizing the time measure, the time span for the model system can be arbitrarily chosen and, for this study, a time scale of 20 times to the prototype value is chosen, which is a compromise between instrumental and practical purposes of our experiment interested [11]. By the definition of the dimensionless temperature and using the material properties of the model, the effective temperature of the model experiment can be calculated. From practical point of view, temperature below 280 K is modified to 280 K as in Fig. 5 because temperature below 280 K would negligibly contribute to the creep while it is convenient to set the initial temperature as that of environment.

For the model condition, the temperature of the environment, 280 K, and a heat transfer coefficient to the environment of 15 W/m^2K are chosen. It is noted that the wall of the model lower head is thin enough to approximate the wall temperature as a lumped value. In this case, the time dependent heat flux can be approximated as the sum of temperature rising term and outgoing wall heat flux as in the Eq. (14) [12].

$$q(t) = (b-a)\rho C_p \frac{\partial T(t)}{\partial t} + \frac{b^2}{a^2} h\{T(t) - T_\infty\} \tag{14}$$

The heat flux calculated for the model experiment of Fig. 6 shows linearly increasing behavior to about 6 kW/m^2.

6. Results and Discussion

In order to demonstrate in a simple manner that dimensional analyses actually works and that tests on a model will predict prototype results, finite element analyses for both prototype and model systems were conducted. The RPV vessel, as seen in Fig. 1, consisted of hemispherical shell which expand freely as being heated and pressurized inside. Both systems were modeled using Eq. (7) and ABAQUS 5.8 with two-dimensional axisymmetric elements [13]. The lower head was modeled with 90 elements (one for each angular degree) and five radial elements, to include a total of 450 elements. In ABAQUS, the computational elements were defined as a “CAX8R,” which are eight noded quadrilateral elements with reduced integration points. This nodalization is expected to give accurate resolution to the stress gradients expected in the analyses [7].

The computational model simulated the boundary condition by restraining the equatorial nodes so that they could not move vertically but could slide freely in radial direction. A pressure boundary condition was established on the inside surface elements. A separate heat flux was specified for the inside elements of prototype and model systems, respectively. Tensile property inputs of Table 1 to ABAQUS are used. The creep model was specified as a user subroutine that returns the creep strain rate as a function of the instantaneous temperature and stress states. It is assumed that the von Mises yield criterion and the Prandtl-Reuss relations, originally derived for plasticity, are valid during creep [14].

Vertical displacement of the bottom apex is often the principal measurement in experiments against which model predictions are compared. The vertical displacements occur in two stages: the first is characterized by a small and linear displacement rate and the second is characterized by a large and accelerating displacement rate [7]. The period of linear displacement rate is associated mainly with thermal expansion as the vessel is heated up. The second stage is characterized by large and accelerating deformation rate due to creep and is of greater interest in severe accident assessments. Defining the dimensionless displacement X, as the vertical displacement of the vessel outer wall divided by its outer radius b, Fig. 7 shows the deformation behavior of prototype and model systems on dimensionless coordinates. Both dimensionless displacements of prototype and model systems predicted well-matched deformation behaviors. Discrepancy at the first stage is due to the larger thermal expansion of the prototype system that is not directly considered as a scaling parameter. That is also noted from the creep strains of Fig. 8 for both prototype and model systems where the two circumferential strains are well collapsed.

7. Conclusions

Dimensional analysis was performed via nondimensionalizing the creep related variables. Dissimilar material modeling is to perform RPV creep deformation experiment in a relatively low pressure and heat flux conditions using a model material. Using the lead (Pb) as the model material, typical conditions of pressure and heat flux of the actual lower head are reduced to:

- pressure: from 2 MPa to 0.187 MPa
- heat flux: from 418 kW/m^2 to under about 6 kW/m^2

Verifications using FEM showed that both nondimensionalized deformation behaviors of prototype and model vessels showed well matched behaviors. As for the future development, angular dependent heat flux may be incorporated into the analysis.

Acknowledgements

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Table 1. Parameters and their values of prototype and model systems

Parameters	Prototype	Model
Density, C_p (kg/m^3)	7,700[a]	11,340[d]
Specific heat, k (J/kgK)	800[a]	129[d]
Conductivity, ρ (W/mK)	33[a]	33[d]
Elastic modulus, E (GPa)	150[a]	14[d]
Thermal expansion coefficient, α (m/m)	15E-6[a]	31.3E-6[d]
Heat transfer coefficient, h (W/m^{2}K)	30[b]	15[e]
Outer temperature, T_∞ (K)	313[b]	280[e]
Outside radius, b (m)	2.536[c]	0.075[f]
Inside radius, a (m)	2.371[c]	0.070[f]

a: Reference [15]
b: Reference [10]
c: Typical value for the lower head geometry of KNGR
d: Reference [16]
e: Room temperature condition for this study
f: 1/34 scale to the prototype

Table 2. Creep constants of prototype and model systems

Creep constants	Prototype [9]	Model [17]
A (h^{-1}MPa^{-m})	3.47E8	2.30E3
m (N/A)	3.74	2.26
Q/R (K)	37,269	6,202

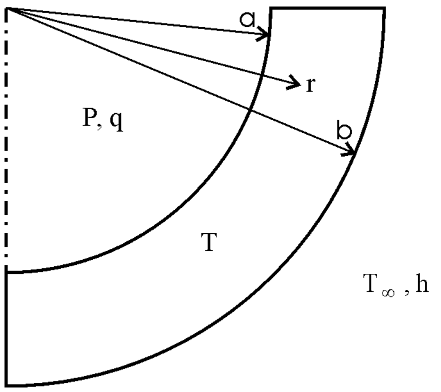

Fig. 1. Schematic of RPV geometry with heat flux q, pressure P, and outside heat transfer boundary conditions T_∞ and h

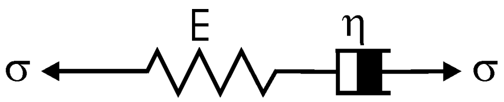

Fig. 2. One dimensional elastic-creep rheology model

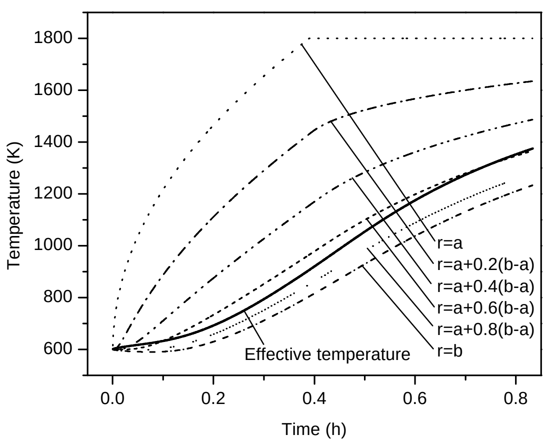

Fig. 3. Temperatures across the vessel and their effective temperature (heat flux, q = 418 kW/m^2)

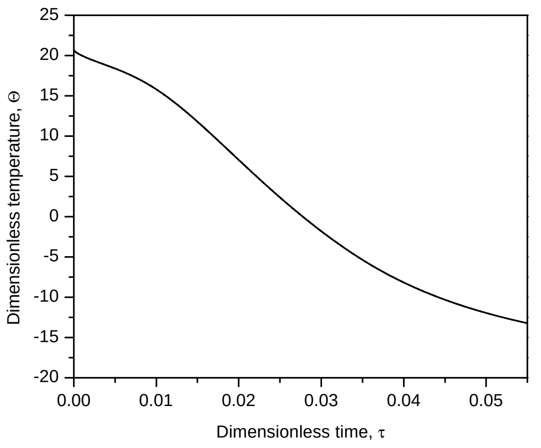

Fig. 4. Dimensionless temperature vs. dimensionless time (heat flux, q = 418 kW/m^2)

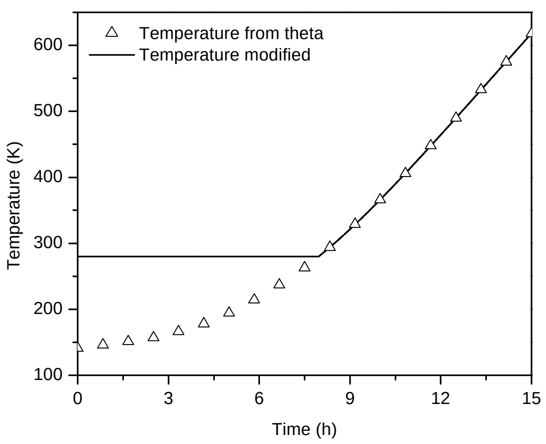

Fig. 5. Temperature of the model system (calculated: symbols, modified: solid line)

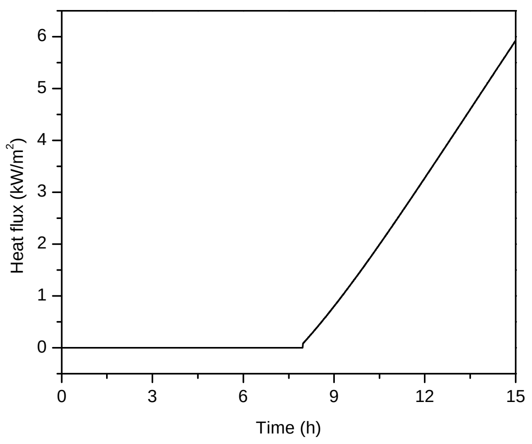

Fig. 6. Calculated heat flux of the model system

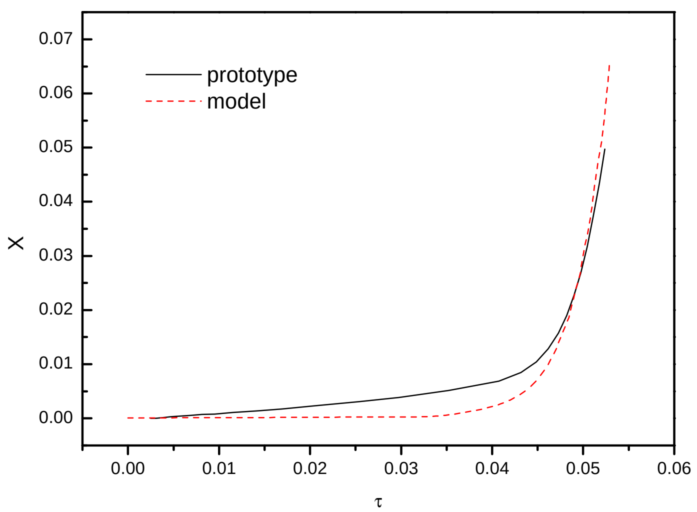

Fig. 7. Elastic-thermal-creep numerical analysis results of dimensionless vertical displacements vs. dimensionless time (prototype: solid line, model: dashed line)

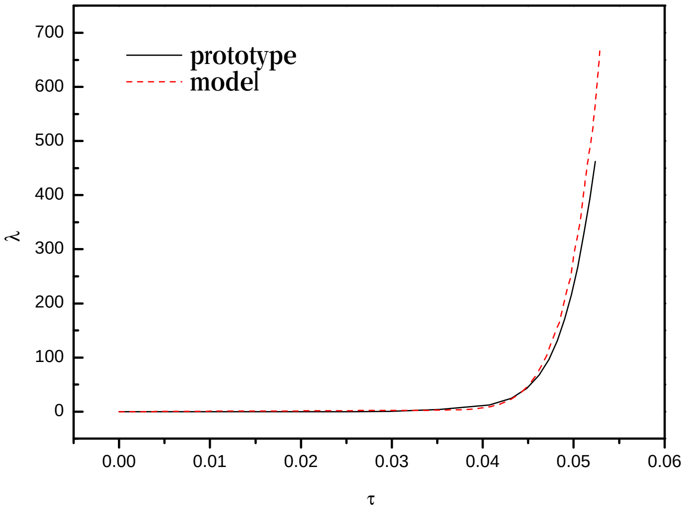

Fig. 8. Elastic-thermal-creep numerical analysis results of dimensionless circumferential creep strains vs. dimensionless time (prototype: solid line, model: dashed line)