General paper

Inelastic Analysis and Thermal Fatigue Life Prediction of a Perforated Cylinder Based on the Effective Stress Concept

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Abstract: In order to examine the applicability of the effective-stress-based analysis method of perforated plates that is proposed by the authors, inelastic behavior and creep-fatigue life of a perforated cylinder subjected to cyclic thermal stress were predicted. Firstly, basic creep behavior of a perforated cylinder under axial tension at uniform temperature was examined by means of three-dimensional FEM creep analysis, and the results showed that macroscopic and local creep behavior of a perforated cylinder is predictable using the proposed method on perforated plates. Secondly, thermal fatigue testing was carried out on a perforated cylinder, and the elastic-plastic-creep behavior of the cylinder was numerically analyzed by modeling the perforated cylinder to the equivalent solid cylinder based on the effective stress concept. Predicted local stress-strain behavior around circular holes was used for creep-fatigue life prediction based on the linear damage rule. Comparison of numerical results and experimentally observed damage clarified that the difference in damage mode at the inner and outer surfaces of a perforated cylinder could be successfully reproduced.

Key Words: *Inelastic analysis, Constitutive equation, Perforated plate, Thermal fatigue, Creep-fatigue life prediction*

1. INTRODUCTION

Perforated structures in high-temperature heat exchangers have low stiffness and stress concentration due to the existence of circular holes, and therefore are often critical parts from the viewpoint of structural design. The development of an inelastic analysis method of perforated plates is greatly needed for predicting creep-fatigue damage of these structures. A detailed FEM inelastic analysis, considering the existence of circular holes and assuming the constitutive modeling of materials, is of course possible. At the same time, a simplified inelastic analysis method, modeling a perforated plate as an equivalent solid plate with the same stiffness, is also adopted in many cases for practical design purposes. Prediction methods of macroscopic Young's modulus, Poisson's ratio and local stress concentrations around circular holes in elasticity are shown in ASME Boiler and Pressure Vessel Code A-8000 (A-8000) [1]. In plasticity and creep conditions, the same approach is adopted on the basis of the equivalent solid plate concept, and studies were performed on the macroscopic constitutive equation and the stress-strain concentrations around circular holes of perforated plates.

Previous works on plasticity are reviewed in the literature [2]; these include the yield surface of perforated plates, plastic properties of equivalent solid plates, limit analysis of perforated plates, stress-strain concentrations around circular holes, and constitutive modeling of perforated plates as a transversely isotropic solid. In terms of creep, on the other hand, there are a few previous reports according to the above review [2]; namely equivalent solid plate properties and stress-strain concentrations around circular holes assuming Norton's creep law [3-4].

Igari et al. [5-7] also assumed Norton's creep law, and proposed advanced prediction methods for both equivalent solid plate properties and stress-strain concentrations, which covered wider range of materials and temperatures than previous works.

Another approach on the basis of the effective stress concept has also been examined [8]; the focus of this approach is the applicability to various types of constitutive equations. Igari et al. [9] demonstrated that the creep behavior of perforated plates exhibiting both primary and secondary creep could be predicted on the basis of effective stress defined for the secondary creep condition. The monotonic and cyclic stress-strain curves of perforated plates in plasticity could also be predicted by assuming the analogy between creep and plasticity [10-11]. Macroscopic relaxation behavior of perforated plates during strain hold in creep-fatigue conditions was also predictable using this approach, and creep-fatigue crack initiation life prediction was carried out using the stress-strain concentrations around circular holes [10-11]. This series of studies was performed mainly for conditions of in-plane loading of constant load on perforated plates.

Actual perforated plate structures experience a cycling of thermal stress due to temperature distributions in the structures, which generally cause multiaxial stress conditions. In order to predict the stress-strain behavior and the crack initiation life of perforated plates in these conditions, a procedure combining both finite element methods (FEM) and the above proposals on perforated plates is necessary. The applicability of this procedure, however, has not been demonstrated.

Firstly in this paper, macroscopic and local creep behavior was obtained through three-dimensional FEM

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creep analysis of the unit section of a perforated cylinder, and the results obtained were compared with those of perforated plates. Secondly, the thermal fatigue test was carried out on a perforated cylinder, in which the uniform heating at a maximum temperature of 950°C and partial cooling were cyclically loaded. The time history of temperature distributions during the test was measured, and the damage to inner and outer surfaces of the perforated cylinder was observed after the test. Lastly thermo-inelastic analysis of the perforated cylinder was performed after modeling it as an axi-symmetric cylinder having an equivalent solid part. Multiaxial damage and crack initiation life were predicted on the basis of the analysis described above, and the results obtained were compared with experimental observations.

2. CONSTITUTIVE EQUATION OF PERFORATED PLATES

2.1. Macroscopic Behavior

2.1.1. Uniaxial deformation

Let us consider that a perforated plate, with a triangular penetration pattern of circular holes, is subjected to uniaxial tension as shown in Fig.1. There are two types of approaches in discussing the macroscopic behavior of perforated plates [8]. One is the equivalent solid plate approach considering the same section area and lower stiffness, giving the same deformation as the perforated plate. The other is the effective stress approach considering the smaller section area and the same constitutive equation as the base material, also giving the same deformation as the perforated plate. Effective stress, often being used in damage mechanics [12-13], is a type of reference stress [14], and is flexible with respect to various types of constitutive equations for predicting the macroscopic behavior of perforated plates.

As the first step to apply these methods to FEM, a constitutive equation in uniaxial loading as shown in Fig.1 is firstly discussed. Results of the review of constitutive equations in the plasticity-creep regime through benchmark testing have been reported [15]. We selected the superposition model to describe the inelastic deformation behavior of both the base material and perforated plates.

The macroscopic behavior of perforated plates under uniaxial tension in the plastic and creep regime is expressed as follows, in comparison with the behavior of the material composing the perforated plates:

$$\varepsilon = \varepsilon_e + \varepsilon_p + \varepsilon_c \quad (\text{Base material}), \quad (1)$$

$$\varepsilon^* = \varepsilon_e^* + \varepsilon_p^* + \varepsilon_c^* \quad (\text{Equivalent solid plate}), \quad (2)$$

where the superfix * denotes the properties of the equivalent solid plate, and ε_e , ε_p and ε_c denote elastic, plastic and creep strain, respectively.

The elastic strain is expressed by Eq.(3) using the effective stress in Eq.(4).

$$\varepsilon_e^* = \sigma^* / E^* = \sigma_{\text{nom}} / E, \quad (3)$$

Fig.1. Equivalent-solid-plate approach and effective stress approach.

$$\sigma_{\text{nom}} = (E/E^*) \sigma^*, \quad (4)$$

where E and E^* are respectively Young's modulus of the base material and the equivalent solid plate, with the ratio E^*/E given in A-8000 [1].

The plastic strain of the base material and the equivalent solid plate are given by the following equations.

$$\varepsilon_p = B \sigma^m, \quad (5)$$

$$\varepsilon_p^* = B^* \sigma^{*m}, \quad (6)$$

where B and m are material constants, and the ratio B^*/B is given by Eq.(7) considering the analogy between creep and plasticity [10-11].

$$B^*/B = (0.591/m + 0.316) \eta^{(-1.18m)}, \quad (7)$$

where η is the ligament efficiency defined by h/p in Fig.1.

Let us assume that the relation between the plastic strain and the stress of the base material is generally depicted by Eq. (8),

$$\varepsilon_p = g(\sigma). \quad (8)$$

Then the relation between the plastic strain and the stress of the equivalent solid plate is expressed by Eq.(9), using the effective stress in Eq.(10) on the basis of Eq.(7) and considering the analogy between creep and plasticity [10-11].

$$\varepsilon_p^* = g(\sigma_{\text{nom}}). \quad (9)$$

$$\sigma_{\text{nom}} = (B^*/B)^{(1/m)} \sigma^*. \quad (10)$$

Regarding creep strain, the base material exhibiting both primary and secondary creep is considered as follows:

$$\varepsilon_c = f(\sigma, t) = P \sigma^q t^r + A \sigma^n t, \quad (11)$$

where P , q , r and t are material constants and time,

respectively. The creep strain of the equivalent solid plate is given by Eq.(12) using the same functional form as Eq. (11).

$$\varepsilon_c^* = f(\sigma_{\text{nom}}, t) = P\sigma_{\text{nom}}^q t^r + A\sigma_{\text{nom}}^n t, \quad (12)$$

where the effective stress σ_{nom} is given in Eq.(15) and (16), assuming Eq.(13) and (14) as the creep properties of the base material and equivalent solid plate, respectively.

$$\dot{\varepsilon}_c = A\sigma^n \quad (\text{Base material}), \quad (13)$$

$$\dot{\varepsilon}_c^* = A^* \sigma^{*n} \quad (\text{Equivalent solid plate}), \quad (14)$$

$$\sigma_{\text{nom}} = (A^*/A)^{(1/n)} \sigma^*, \quad (15)$$

$$A^*/A = (0.591/n + 0.316)\eta^{(-1.18n)}. \quad (16)$$

The suitability of Eq.(12) in reproducing the macroscopic creep behavior of perforated plates exhibiting both primary and secondary creep is shown in ref. [8-9]. The above descriptions are based on the uniaxial constitutive equation of perforated plates.

Here, we would like to discuss the suitability of Eq. (4), the approximate solution of the effective stress for Eq. (10) in plasticity and Eq.(15) in creep. In ref.[7], the effective stress in Eq.(15) in creep was compared with the approximate solution in Eq.(4). The results were as follows: For the case of $1/n=1.0$, Eq.(15) and (4) matched well; for $1/n$ from 0.1 to 0.2 corresponding to the exponent n from 5 to 10, the discrepancy was under 5% ; the discrepancy was under 10% for other values of n . A similar approximation is expected in the case of plasticity when using the approximate solution of effective stress as a common parameter for describing the macroscopic inelastic behavior of perforated plates.

2.1.2. Multiaxial deformation

To be precise, there is a small anisotropy of macroscopic deformation in the plane of perforated plates with a triangular penetration pattern of circular holes, and the deformation characteristics in the perpendicular axis to the plane are different from those in the plane of perforated plates. Two types of simplification of this anisotropy are often adopted. One is the transversely isotropic modeling of perforated plates [16], and the other is the isotropic modeling used in A-8000. This isotropic modeling is based on the experimental results of both the tensile tests and the out-of plane bending tests of perforated plates with a thickness of over twice the hole pitch. In this paper the assumption of the isotropy in A-8000 is adopted for the modeling of multiaxial deformation of perforated plates.

Concerning the macroscopic Poisson's ratio of perforated plates, it is assumed that Poisson's ratio in A-8000 is used for elasticity, and that there is no volume change in plasticity and creep.

Using the above assumptions, the same procedure as for the base material can be applied to the structural anal-

ysis of perforated plates in multiaxial stress conditions. In other words, the von Mises initial yield condition, the associated flow rule and the kinematic hardening rule are adopted in plasticity ; primary and secondary creep together with the strain hardening rule are considered in creep.

2.2. Local Stress-Strain Concentration

The maximum local stress σ_{max} and strain ε_{max} in the tangential direction around circular holes are uniaxial, and ε_{max} can be expressed as follows:

$$\varepsilon_{\text{max}} = \varepsilon_{\text{emax}} + \varepsilon_{\text{pmax}} + \varepsilon_{\text{cmax}} = K_t \varepsilon_e^* + K_{\varepsilon_p} \varepsilon_p^* + K_{\varepsilon_c} \varepsilon_c^*, \quad (17)$$

where $\varepsilon_{\text{emax}}$, $\varepsilon_{\text{pmax}}$ and $\varepsilon_{\text{cmax}}$ are the maximum values of elastic, plastic and creep strain, respectively.

The elastic strain concentration factor is expressed as follows:

$$K_t = E^* Y_{\text{max}} / (E\eta), \quad (18)$$

where $Y_{\text{max}} (= \sigma_{\text{max}} / (\bar{\sigma}^* / \eta))$ is a stress multiplier in A-8000 depending on multiaxial stress conditions, and is determined on the basis of the equivalent stress by von Mises $\bar{\sigma}^*$.

The plastic strain concentration factor K_{ε_p} on the basis of the equivalent plastic strain by von Mises, is expressed in Eq.(19) through Eq.(20) considering the analogy between plasticity and creep [10-11].

$$K_{\varepsilon_p} = \varepsilon_{\text{pmax}} / \bar{\varepsilon}_p^* = (CK_t)^{(2m/(m+1))}, \quad (19)$$

$$C = (-1.359 \log_{10} \eta - 0.551) (1/m - 1) + 1. \quad (20)$$

The creep strain concentration factor K_{ε_c} in Eq.(21) on the basis of Norton's creep law and Neuber's rule gives the upper bound even for the case with both primary and secondary creep.

$$K_{\varepsilon_c} = \varepsilon_{\text{cmax}} / \bar{\varepsilon}_c^* = (CK_t)^{(2n/(n+1))}, \quad (21)$$

where $\bar{\varepsilon}_c^*$ is the equivalent creep strain by von Mises, and factor C in Eq.(21) is obtained from Eq.(20) by replacing m with n .

The stress concentration factor during the relaxation period is given by the following equation:

$$K_{\sigma} = \sigma_{\text{max}} / (\bar{\sigma}^* / \eta) = \eta (CK_t)^{(2/(n+1))} (A^*/A)^{(1/n)}. \quad (22)$$

3. THREE-DIMENSIONAL CREEP ANALYSIS OF PERFORATED CYLINDER

The analysis model is shown in Fig.2. Two types of thick-walled cylinder with the same outer radius are considered: cylinder 1 with a thickness of 85mm and cylinder 2 with a thickness of 142.5mm. Eighteen circular holes are located in the radial direction from the center of the cylinder, and the axial pitch of the holes is determined so that the penetration pattern in the mid-wall thickness is triangular. The pitch of the holes in the circumferential

direction on the outer and inner surfaces is different from that in mid-wall thickness, such that the penetration patterns are not perfectly triangular. The creep deformation behavior of these cylinders subject to axial tension was numerically analyzed to examine the applicability of the prediction methods on perforated plates in Section 2 above.

Two kinds of ligament efficiency η in the mid-wall thickness, 0.5 and 0.7, were considered. The unit volume, shown in the shadowed area in Fig.2 as an example of $\eta = 0.7$ for cylinder 1, was divided into FEM meshes. The program MARC and iso-parametric elements with 20 nodes were used; the total numbers of elements and nodes were 360 and 2027, respectively. Young's modulus of 1.329×10^5 MPa and Poisson's ratio of 0.3 were used as material parameters in the numerical analysis, and the following creep strain rate was used.

$$\dot{\epsilon}_c = 1.678 \times 10^{-10} \sigma^{4.45}, \quad (23)$$

where units of strain and time are mm/mm and h, respectively.

The boundary conditions are shown in Fig. 3. The periodic symmetry of circumferential deformation in the planes AFLG and CDJI was considered, and the axial deformation in the z-direction of the plane ABHG was set as uniform. Under these conditions, axial force was loaded to the plane ABHG.

Fig.2. FEM mesh division for perforated cylinder.

Fig.3. Boundary conditions.

Fig.4. Macroscopic creep behavior of perforated cylinder.

Fig.5. Local stress around a hole.

Young's modulus for the equivalent solid cylinder E^* was obtained by dividing the stress of the equivalent solid plate σ^* by its axial strain ϵ^* , where σ^* is the axial force divided by the section area neglecting the existence of the holes. Poisson's ratio ν^* was determined from the radial displacement of the plane of mid-wall thickness.

Young's modulus E^* of the perforated cylinder was about 2% higher than the values of the perforated plates in both cases of $\eta = 0.5$ and 0.7 . The Poisson's ratio ν^* was nearly the same as those of perforated plates.

The macroscopic creep deformation of the perforated cylinder with ligament efficiency of 0.7 in the mid-wall thickness is shown in Fig.4, together with those of a perforated plate with the same ligament efficiency. The macroscopic creep strain of the perforated plate is slightly higher than that of the perforated cylinder, but the macroscopic creep deformation behavior of the perforated cylinder was well predicted based on the methods for perforated plates.

The local stress concentration of the perforated plate and perforated cylinder are compared in Fig.5. The maximum stress in the perforated cylinder had a distribution throughout the wall thickness; that in the mid-wall thickness in the "plane-strain" condition was higher than those in the inner and outer radius in the "plane-stress" condition. The maximum stress in the outer radius was higher than that in the inner radius, and this comes from the fact that the circumferential pitch of the holes in the

outer radius is greater, resulting in a larger stress concentration in the outer radius. The maximum stress after creep in the perforated plate under the same stress, shown in the dotted line in the figure, nearly matched with the maximum stresses in the outer and inner radius.

The above results show that the macroscopic and local stress and strain behavior of a perforated cylinder can be predicted on the basis of those of perforated plates with the same ligament efficiency at the mid-wall thickness of the perforated cylinder.

4. THERMAL FATIGUE LIFE PREDICTION OF PERFORATED CYLINDER

4.1. Test Method

A thick-wall cylinder of Hastelloy X with an outer diameter of 57mm, a thickness of 8mm and a length of 250mm was machined from a round bar with a diameter of 60mm. Tensile strength, yield stress defined by 0.2% offset plastic strain and elongation of the tested material at room temperature were, respectively, 686MPa, 325 MPa and 52%. As shown in Fig.6, eighteen circular holes with a diameter of 2.5mm were machined in the radial direction along the circumference of the cylinder.

The penetration pattern of circular holes was triangular with ligament efficiency of $\eta=0.7$ at the mid-wall thickness. There were fifteen layers of holes in the axial direction. Figure 7 shows the test method schematically. Firstly the test specimen was heated to the uniform temperature of 950°C in an electric furnace in an air environment, and was then cooled by means of compressed air flowing into the cylinder and flowing out of the holes of the cylinder. Axial temperature distribution occurred in the cooling process because the perforated portion in the middle of the axial length of the cylinder (i.e., the ligament portion) was cooled faster than the part without holes (i.e., the rim portion). The test spe-

Fig.6. Perforated cylinder for thermal fatigue test.

Fig.7. Test method.

cimen was again heated up slowly to the uniform temperature of 950°C and was kept at this temperature for 30 minutes. The total time in one cycle was 45 minutes and this process was cycled. The axial temperature distribution at the outer surface was measured by arranging 19 thermocouples set straight in the axial direction of the outer surface of the cylinder. With respect to the temperature distributions at the center of ligament and at the rim-ligament boundary, 12 thermocouples were set; 4 were set at the outer circumference and 1 at the inner circumference.

4.2. Test Results

The axial temperature distributions at 6 and 18 seconds after the start of cooling are shown in Fig.8. Cooling of the ligament portion with holes was faster than that of the rim portion without holes. The temperature distribution in the ligament portion was nearly uniform as seen from the figure. The temperature difference between the outer and inner surfaces was around 10°C. The temperature difference along the circumference of the cylinder was small, and could be regarded as axi-symmetric.

The total number of test cycles was 1200, and the damage around the holes in the inner and outer surfaces was examined by using the replication method at 400, 800, 1000 and 1200 test cycles. This surface examination was carried out at the center of the cylinder, and at the rim-ligament boundaries (meaning the edge of the

Fig.8. Temperature distribution along the length of specimen.

Fig.9. Damage around holes of inner and outer surfaces.

perforated portion). A view of the damage on both the inner and outer surfaces of the rim-ligament boundaries is inner surface, on the other hand, transgranular cracks in radial direction from the center of the circular holes were found. Microscopic cracks up to 0.2~0.5mm were found at 800 cycles on the inner surface and at 1000 cycles on the outer surface.

5. INELASTIC ANALYSIS AND LIFE PREDICTION

5.1. Method of Numerical Analysis

The half-length of the specimen was modeled as an axi-symmetric cylinder as shown in Fig.10. SENSOR/NSAS code and isoparametric elements were used in the numerical analysis. In the heat conduction analysis, circular holes were modeled as the equivalent slits, in which both heat conduction and heat transfer from the circular holes were considered. The thickness of the slit was equal to the diameter of the hole, and the thermal conductivity in this slit was set as that of the base material multiplied by the ligament efficiency. The density of the body in the slit, required for transient heat conduction analysis, was also determined considering the reduction due to the existence of the holes. The heat transfer coefficient was determined for reproducing the measured temperature distribution in the cooling process. The numerical results of heat conduction, written as open symbols in Fig.8, corresponded comparatively well to the measured results.

Inelastic numerical analysis was performed based on the numerical results of temperature distribution. The ligament portion in Fig.10 was modeled to the equivalent solid. The assumptions for describing the inelastic behavior of the equivalent solid under multiaxial stress conditions are the same as those for normal metallic materials, such as initial isotropy, associated flow rule and incompressivity, as shown in Section 2.1. The elastic-plastic-creep analysis was carried out considering the temperature dependence of the material properties. Young's modulus and Poisson's ratio in A-8000 were used as the elastic properties, and the ratio of Young's modulus E^*/E for $\eta=0.7$ was 0.73. Regarding the equivalent solid, yield stress and flow stress of the base material in plasticity were reduced by the ratio of 0.73 on the basis of Eqs.(4) and (9). The elastic-plastic behavior of the base material and the equivalent solid was modeled as a bilinear shape with temperature dependence. Concerning the creep behavior of both materials, both primary and secondary creep were considered in Eq.(12) together with the effective stress $\sigma_{nom}=1.37\sigma^*$ in Eq.(4), where 1.37 is the inverse value of $E^*/E=0.73$. Elastic-plastic analysis using the kinematic-hardening rule

was applied to the cooling process, and the creep analysis using the strain-hardening rule was applied to the hold time at a uniform temperature of 950°C. Inelastic analysis for three thermal cycles was carried out, and the prediction methods in Section 2.2 were applied to predict local stress and strain concentrations.

5.2. Results of Numerical Analysis

Examples of elastic stress at 18 seconds from the start of cooling from the uniform temperature of 950°C are shown in Fig.11. The ligament side at the rim-ligament boundary was the most critical part; the inner surface of this part was subjected to the biaxial tension of axial and hoop stress, while the outer surface of this part was subjected to uniaxial compression of axial stress. The cycling of this thermal stress caused fatigue damage. Since the thermal stress in the cooling process exceeded the yield stress of the equivalent solid, plastic strain occurred, resulting in the residual stress after the cooling process was finished. This residual stress remains after heating to the uniform temperature of 950°C, causing creep damage during the holding time at this temperature.

The axial stress-strain hysteresis loops were obtained from inelastic analysis on the inner and outer surfaces of the ligament side of the rim-ligament boundary, as shown in Fig.12. On the outer surface, the axial stress during the hold period at 950°C was found to be tensile. As shown in Fig.11 the hoop stress was negligible on the outer surface at this portion. On the inner surface, however, the axial stress during the hold period at 950°C was found to be compressive. As also shown in Fig.11, the hoop stress on the inner surface from elastic analysis had the same sign and about the same magnitude as the axial stress. This fact corresponded well with the microscopic damage in Fig.9. Although both outer and inner surfaces experienced multiaxial creep-fatigue, the inner surface was subjected to biaxial loading together with compressive creep resulting in the transgranular cracks in the radial direction of the circular holes. The outer surface, on the other hand, was subjected to uniaxial loading with tensile hold resulting in the intergranular cracks from axial stress.

Creep-fatigue life prediction was carried out on the basis of the Robinson-Taira linear damage rule [17-18], where a failure of material is assumed to occur when the summation of fatigue and creep damage reaches unity. Fatigue damage was estimated using both the local strain range from Eq.(17) and the fatigue data of the base material. Creep damage during the hold time at the uniform temperature of 950°C was estimated using both the local stress from Eq.(22) and the creep rupture data of the base material. Although there are several review papers on multiaxial creep-fatigue life prediction [19], the linear damage rule was used here as a simple and widely used method for the purpose of practical design. Creep damage was estimated during the hold time at the uniform temperature of 950°C and during the heating process to 950°C.

The predicted lives of the inner and outer surfaces were 305 and 323 cycles, respectively, which were more

Fig.10. FEM model of perforated cylinder.

Fig.11. Elastic stress distribution along the length of specimen.

Fig.12. Stress-strain hysteresis at inner and outer surfaces of rim-ligament boundary.

Fig.13. Results of creep-fatigue life prediction.

conservative than the experimental values, 800 and 1000 cycles, respectively. When compared with the case of creep-fatigue tests of perforated plates at the uniform temperature of 950°C [10-11], predicted life was slightly shorter than experimental results, as shown in Fig.13. Since the experimentally observed life of a perforated cylinder generally depends on the crack length and can have some scatter band, a procedure involving both effective stress-based inelastic analysis and creep-fatigue life prediction is considered as being applicable for practical

design purposes. The development of inelastic analysis using nonlinear plasticity models or unified constitutive models together with a life prediction method such as the strain range partitioning method is also possible in the future.

6. CONCLUSIONS

The effective-stress-based inelastic analysis method for perforated plates was applied to inelastic analysis and thermal fatigue life prediction of a perforated cylinder. The results can be summarized as follows:

- (1) Macroscopic creep behavior and local stress around the circular holes of a perforated cylinder having a triangular penetration pattern of circular holes at the mid-wall thickness could be predicted using the proposed methods for perforated plates.
- (2) The numerically obtained stress-strain behavior of the perforated cylinder corresponded to the damage on the inner and outer surfaces. Predicted life was conservative by a factor of three when compared with experimental results, and prediction methods were considered as being applicable for practical design purposes.

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