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Creep-rupturing of open-cell foams

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Abstract

A repeating element consisting of four straight and uniform-thickness cell struts in a pentagonal dodecahedron model is employed to analyze theoretically the creep-rupturing of open-cell foams. In the repeating element, the solid making up cell struts is assumed to follow power-law creep and the Monkman–Grant relationship. Consequently, the theoretical expressions for describing the steady-state creep strain rate and creep-rupturing time of open-cell foams are obtained. It is shown that the creep-rupturing of open-cell foams can also be described by the Monkman–Grant relationship. Moreover, the Monkman–Grant parameters m^* and B^* of open-cell foams depend on their cell structure and those of solid cell struts. The Monkman–Grant parameters determined from the existing experimental results on the creep-rupturing of open-cell aluminum alloy foams are compared to those calculated theoretically from the proposed pentagonal dodecahedron model. The difference between theoretically calculated and empirically determined B^* is attributed to some pre-existing cell structural imperfections in open-cell aluminum alloy foams.

Keywords: Open-cell foam; Cell structure; Creep; Rupturing

1. Introduction

Experimental results on the creep strain rates of ceramic, polymeric and metallic foams have been reported by many researchers [1–4]. Also, the theoretical expression for describing the creep strain rate of foams was derived by Andrews et al. [4] from a cell-strut-bending cubic model proposed by Gibson and Ashby [5]. Both theoretical and experimental results indicate that the creep strain rates of foams are significantly affected by their relative density and the creep parameters of solid cell struts. Furthermore, the creep buckling of foams subjected to a uniaxial compressive stress was analyzed theoretically by Cocks and Ashby [6] from the cell-strut-bending cubic model with straight and uniform-thickness cell struts. However, non-uniform cell-strut cross-section and non-straight profile of cell struts are typically observed in real foams. For example, the processing of aluminum foams from a liquid to a solid state will affect the solid distribution in cell struts, leading to formations of non-uniform cell-strut cross-section and non-straight profile of cell struts. Hexagonal honeycombs with regular, repeated and simple cell geometry are frequently used as models for evaluating the effects of cell structural imperfections on the mechanical properties of foams with complex and irregular strut morphology. The role of cell structure in creep of hexagonal honeycombs was numerically studied by Andrews and Gibson [7] while the creep strain rates and creep-buckling times of hexagonal honeycombs with variable-thickness cell struts were theoretically analyzed by Lin and Huang [8,9]. It was found that the effects of cell structural imperfections on the creep strain rate and creep buckling of two-dimensional cellular materials are significant and cannot be neglected in estimating their service life.

The experimental results on the creep-rupturing times of aluminum foams presented by Andrews et al. [3,4] were found to follow the well-known Monkman–Grant relationship. However, the theoretical analysis for the creep-rupturing of foams has not been paid much attention. In this

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article, we aim at analyzing theoretically the creep-rupturing of open-cell foams. At first, a repeating element composed of four uniform-thickness and straight cell struts in a pentagonal dodecahedron model is used to analyze the creep-rupturing of open-cell foams. Then, the existing experimental results on the creep-rupturing of open-cell aluminum foams [4] are compared to the proposed theoretical model. As a result, the validity of the proposed theoretical model for describing the creep-rupturing of open-cell foams is evaluated.

2. Theoretical analysis

A pentagonal dodecahedron model as shown in Fig. 1 is used to represent the cell geometry of open-cell foams. Within the model foam, a repeating element composed of four cell struts set to meet at equal angles of 108° as schematically illustrated in Fig. 2 is employed to analyze the creep-rupturing of open-cell foams; the repeating element was used earlier to analyze the elastic buckle strength of open-cell foams by Gibson et al. [10]. Thus, the angle of each inclined cell strut in the repeating element measured from the horizontal x_2–x_3 plane is equal to 18°. In the repeating element, each cell strut with a cell length of ℓ and a square cross-sectional area of t^2 is straight and has uniform thickness. Therefore, the relative density of the model foam can be calculated from a simple geometrical analysis and is expressed as

$$\frac{\rho^*}{\rho_s} = C\left(\frac{t}{\ell}\right)^2 \tag{1}$$

where the cell structural constant C of the pentagonal dodecahedron model is found to be 1.305 while ρ^* and ρ_s are the density of the model foam and that of solid cell struts, respectively.

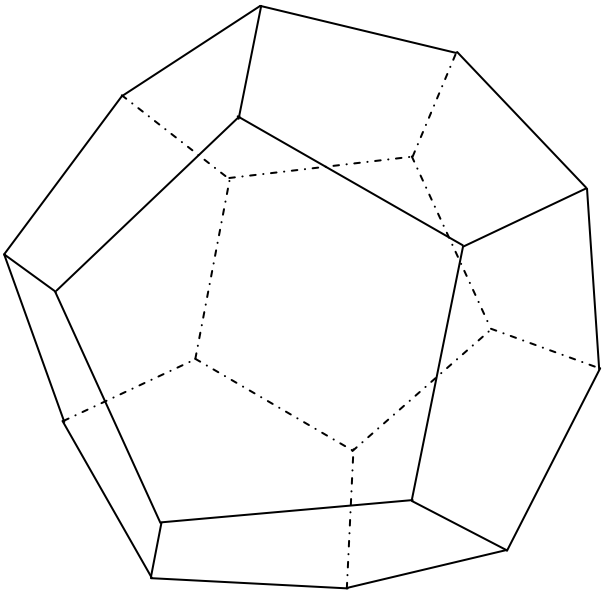

Fig. 1. A pentagonal dodecahedron model is used to represent the cell geometry of open-cell foams.

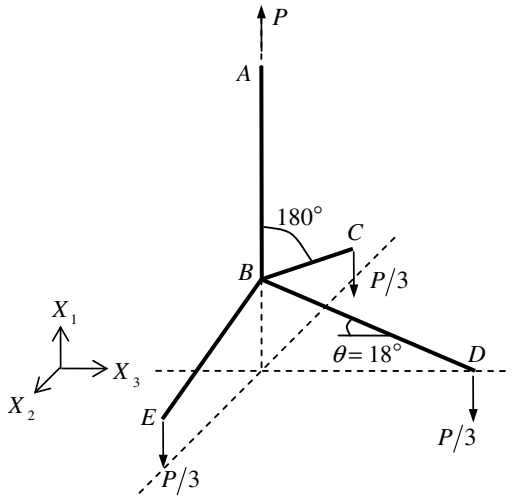

Fig. 2. A repeating element composed of four uniform-thickness and straight cell struts set to meet at equal angles of 108° is employed to analyze the creep-rupturing of open-cell foams.

The creep strain rate of the model foam can be obtained from analyzing the deflection rate of each inclined cell strut if the joint region of the four cell struts in the repeating element of Fig. 2 is set to be rigid, as suggested by Warren and Kraynik [11]. When a remote uniaxial uniform stress σ^* is imposed on the outermost boundary of the model foam along the x_1 direction, the induced axial force acting at the vertical cell strut of the repeating element is $P = C_1\sigma^*\ell^2$ as illustrated in Fig. 2; here, C_1 is another cell structural constant. Furthermore, the induced shear force $V = C_1\sigma^*\ell^2\cos 18°/3$, axial force $N = C_1\sigma^*\ell^2\sin 18°/3$ and bending moment $M_e = C_1\sigma^*\ell^3\cos 18°/6$ acting at both ends of each inclined cell strut as shown in Fig. 3 can be obtained from equilibrium. The bending moment exerted

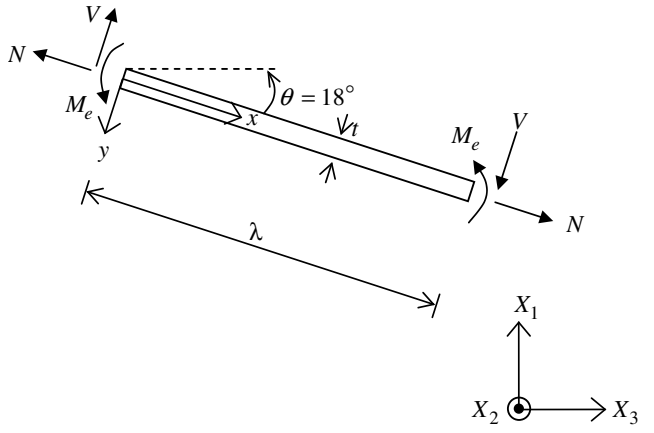

Fig. 3. From equilibrium, the induced shear force $V = C_1\sigma^*\ell^2\cos 18°/3$, axial force $N = C_1\sigma^*\ell^2\sin 18°/3$ and bending moment $M_e = C_1\sigma^*\ell^3\cos 18°/6$ acting at both ends of each inclined cell strut can be obtained.

on any cross-section of each inclined solid cell strut beam can be further expressed as

$$M(x) = \frac{P\cos 18^\circ}{3}\left(x - \frac{\ell}{2}\right) \tag{2}$$

where x is measured from the left side of each inclined solid cell strut beam along its neutral axis as plotted in Fig. 3.

It is assumed that the plane cross-sections of inclined solid cell strut beams remain plane. Hence, the resulting steady-state creep strain rate $\dot{\varepsilon}_s$ at any point of a cross-section of the solid cell strut beam of Fig. 3 can be related to the change rate of its curvature $\dot{\kappa}(x)$:

$$\dot{\varepsilon}_s = -\dot{\kappa}(x)y \tag{3}$$

where y is the distance measured from the neutral axis of the solid cell strut beam. Equilibrium requires that the internal bending moment induced at any cross-section of the solid cell strut beam be related to its normal stress σ_s by

$$M(x) = 2t\int_0^{t/2}\sigma_s y\,\mathrm{d}y \tag{4}$$

In addition, the solid making up the cell struts of the model foam is assumed to follow power-law creep:

$$\dot{\varepsilon}_s = \dot{\varepsilon}_o\left(\frac{\sigma_s}{\sigma_0}\right)^n \tag{5}$$

Here, $\dot{\varepsilon}_o$, σ_0 and n are the creep parameters of solid cell struts. Substituting Eqs. (3) and (5) into Eq. (4) gives

$$\dot{\kappa}(x) = \dot{\kappa}_0\left[\frac{M(x)}{M_0}\right]^n \tag{6}$$

Here, $\dot{\kappa}_0 = -2\dot{\varepsilon}_0/t$ and $M_0 = [2n/(1+2n)]\sigma_0 t^3/4$ are found.

By prescribing the boundary conditions of $\dot{\delta} = 0$ and $\mathrm{d}\dot{\delta}/\mathrm{d}x = 0$ at $x = 0$, the deflection rate $\dot{\delta}(x)$ along the y direction of the inclined solid cell strut beam of Fig. 3 can be calculated when the method of successive integration [12] and the relationship of $\dot{\kappa} = \mathrm{d}^2\dot{\delta}/\mathrm{d}x^2$ in the mechanics of materials are used:

$$\dot{\delta}(x) = \dot{\kappa}_0\left[\frac{P\cos 18^\circ}{3M_0}\right]^n\left[\frac{1}{(n+1)(n+2)}\left(x-\frac{\ell}{2}\right)^{n+2} - \frac{x}{n+1}\left(\frac{\ell}{2}\right)^{n+1} + \frac{1}{(n+1)(n+2)}\left(\frac{\ell}{2}\right)^{n+2}\right] \tag{7}$$

Hence, the maximum deflection rate at $x = \ell$ of the inclined solid cell strut beam can be obtained and written as

$$\dot{\delta} = \dot{\varepsilon}_0\left(\frac{1}{n+2}\right)\left(\frac{1+2n}{2n}\right)^n\left(\frac{\sigma^*}{\sigma_0}\right)^n\left(\frac{C_1\cos 18^\circ}{3}\right)^n\left(\frac{\ell}{t}\right)^{3n}\frac{\ell^2}{t} \tag{8}$$

Furthermore, the steady-state creep strain rate $\dot{\varepsilon}^*$ of the model foam along the loading direction can be calculated from the maximum deflection rate $\dot{\delta}$ of the inclined solid cell strut beam through the relationship of $\dot{\varepsilon}^* = \dot{\delta}\cos 18^\circ/\ell(1+\sin 18^\circ)$. As a result, the steady-state creep strain rate of the model foam can be expressed as

$$\frac{\dot{\varepsilon}^*}{\dot{\varepsilon}_0} = \left(\frac{C^{(3n+1)/2}}{n+2}\cdot\frac{\cos 18^\circ}{1+\sin 18^\circ}\right)\left(\frac{C_1\cos 18^\circ}{3}\right)^n\left(\frac{1+2n}{2n}\right)^n \times\left(\frac{\sigma^*}{\sigma_{0s}}\right)^n\left(\frac{\rho^*}{\rho_s}\right)^{-(3n+1)/2} \tag{9}$$

Next, the solid making up the cell struts of the model foam is assumed to obey the following Monkman–Grant relationship:

$$\log t_{f,s} = -m_s\log\dot{\varepsilon}_s + B_s \tag{10}$$

where $t_{f,s}$ is the creep-rupturing time, $\dot{\varepsilon}_s$ is the steady-state creep strain rate and B_s and m_s are the Monkman–Grant parameters of solid cell struts. It is assumed that a macro-crack initiates and then propagates rapidly when the creep-rupturing of a critical cell strut within the model foam occurs. Hence, the time required to reach the onset of creep-rupturing of the model foam, t_f^*, is identical to that of an inclined solid cell struts, $t_{f,s}$; in other words, $\log t_f^* \approx \log t_{f,s}$.

The maximum steady-state creep strain rate of each inclined solid cell strut beam occurs at $x = \ell$ and can be calculated from Eq. (3):

$$(\dot{\varepsilon}_s)_{\max.} = \dot{\varepsilon}_0\left(\frac{1+2n}{n}\right)^n\left(\frac{C_1C^{3/2}\cos 18^\circ}{3}\right)^n\left(\frac{\sigma^*}{\sigma_0}\right)^n\left(\frac{\rho^*}{\rho_s}\right)^{-3n/2} \tag{11}$$

By substituting Eq. (9) into Eq. (11), the maximum steady-state creep strain rate of each inclined solid cell strut beam can be expressed in terms of the steady-state creep strain rate of the model foam along the loading direction:

$$(\dot{\varepsilon}_s)_{\max.} = \dot{\varepsilon}^*\frac{(n+2)(1+\sin 18^\circ)}{C^{1/2}\cos 18^\circ}\left(\frac{\sigma^*}{\sigma_0}\right)^n\left(\frac{\rho^*}{\rho_s}\right)^{1/2} \tag{12}$$

The creep-rupturing time of the model foam can be found by substituting Eq. (12) into Eq. (10) and then by setting $\log t_f^* \approx \log t_{f,s}$:

$$\begin{aligned}\log t_f^* \approx \log t_{f,s} &= -m_s\log(\dot{\varepsilon}_s)_{\max.} + B_s \\ &= -m_s\log\dot{\varepsilon}^* - m_s\log\left[\frac{(n+2)(1+\sin 18^\circ)}{C^{1/2}\cos 18^\circ}\right] \\ &\quad - m_s\log\left(\frac{\rho^*}{\rho_s}\right)^{1/2} + B_s\end{aligned} \tag{13}$$

The above equation can be rearranged to give the Monkman–Grant relationship for describing the creep-rupturing of the model foam:

$$\log t_f^* = -m^*\log\dot{\varepsilon}^* + B^* \tag{14}$$

$$m^* = m_s \tag{15}$$

$$B^* = -m_s\log\left[\frac{(n+2)(1+\sin 18^\circ)}{C^{1/2}\cos 18^\circ}\right] - m_s\log\left(\frac{\rho^*}{\rho_s}\right)^{1/2} + B_s \tag{16}$$

Here, m^* and B^* are defined as the Monkman–Grant parameters of the model foam. Eqs. (14)–(16) suggest that the creep-rupturing of open-cell foams can be described by the Monkman–Grant relationship. In addition, the Monkman–Grant parameter m^* of open-cell foams is exactly the same as that of solid cell struts, regardless of their cell structure. However, the other Monkman–Grant parameter B^* of open-cell foams depends on that of solid cell struts as well as their cell structure.

3. Results and discussion

3.1. Elastic properties

From Eq. (9), the steady-state creep strain rate of open-cell foams can be calculated once their relative density and the imposed uniaxial tensile or compressive stress are specified. Hence, Eq. (9) can be further reduced to give the elastic modulus E^* of open-cell foams by setting $n = 1$, $\dot{\varepsilon}_0 = 1$ and $\sigma_0 = E_s$:

$$\frac{E^*}{E_s} = \frac{6}{C_1 C^2} \frac{1 + \sin 18^\circ}{\cos^2 18^\circ} \left(\frac{\rho^*}{\rho_s}\right)^2 \tag{17}$$

Here, E_s is the elastic modulus of solid cell struts. For the limit of $n = \infty$ and $\sigma_0 = \sigma_{ys}$, Eq. (9) can be reduced to represent the plastic collapse strength of open-cell foams, σ^*_{pl}:

$$\frac{\sigma^*_{pl}}{\sigma_{ys}} = \frac{3}{C_1 C^{3/2} \cos 18^\circ} \left(\frac{\rho^*}{\rho_s}\right)^{3/2} \tag{18}$$

where σ_{ys} is the yielding strength of solid cell struts.

Eqs. (17) and (18) derived from the pentagonal dodecahedron model of Fig. 1 can be compared to the theoretical results provided by Gibson and Ashby [5] for the elastic modulus and plastic collapse strength of open-cell foams from a cubic cell model:

$$\frac{E^*}{E_s} \approx \left(\frac{\rho^*}{\rho_s}\right)^2 \tag{19}$$

$$\frac{\sigma^*_{pl}}{\sigma_{ys}} \approx 0.3 \left(\frac{\rho^*}{\rho_s}\right)^{3/2} \tag{20}$$

Since $C = 1.305$ was found previously from Eq. (1), C_1 can be determined by comparing Eqs. (17)–(19) or Eqs. (18)–(20). As a result, C_1 is approximately equal to 5.6 when Eqs. (17) and (18) are satisfied simultaneously.

3.2. Existing experimental results

To verify the validity of the proposed pentagonal dodecahedron model for describing the creep-rupturing of open-cell foams, the existing experimental results on the creep strain rates and creep-rupturing times of commercially available open-cell aluminum alloy foams (Duocel, ERG, USA) presented by Andrews et al. [4] are utilized and analyzed here. The solid making up the open-cell foams is 6101-T6 aluminum alloy. In their experimental measurements, three different relative densities of open-cell foams considered were 0.06, 0.09 and 0.14. The resulting steady-state creep strain rates of aluminum alloy foams were plotted with respect to the imposed uniaxial compressive stresses to determine their corresponding power-law creep exponents The measured power-law creep exponents of open-cell aluminum alloy foams with a relative density of 0.06, 0.09 and 0.14 under uniaxial compression were $n = 4.8$, 4.2 and 3.5, respectively, and found to be close to the power-law creep exponent of solid 6101-T6 aluminum alloy $n = 4.4$. Meanwhile, the measured power-law creep exponent of the aluminum alloy foam with a relative density of 0.09 under uniaxial tension was $n = 5.0$ and slightly larger than that under uniaxial compression.

On the other hand, the measured creep-rupturing times t^*_f of aluminum alloy foams were plotted against their corresponding steady-state creep strain rates $\dot{\varepsilon}^*$ in a figure. Consequently, Andrews et al. [4] confirmed that the experimental results on the creep-rupturing of aluminum alloy foams can be described by the Monkman–Grant relationship of Eq. (14). The measured Monkman–Grant parameters of aluminum alloy foams were $m^* = 0.9$, 0.8 and 0.78 and $B^* = -2.039$, -1.103 and -0.968 when their relative densities were 0.06, 0.09 and 0.14, respectively. In addition, the Monkman–Grant parameters of the aluminum alloy foams with a relative density of 0.09 under uniaxial tension were experimentally found to be $m^* = 0.93$ and $B^* = -2.029$. Based on the existing experimental results, it is found that the creep-rupturing of aluminum alloy foams under uniaxial compression is slightly different from that under uniaxial tension.

3.3. Comparison between experimental results and theoretical model

As the creep strain rates and creep-rupturing times of open-cell aluminum alloy foams follow the Monkman–Grant relationship, their corresponding Monkman–Grant parameters can be determined experimentally and then compared to those calculated theoretically from Eqs. (14)–(16). Eqs. (14)–(16) show that the Monkman–Grant parameter m^* of open-cell foams is exactly equal to that of solid cell struts m_s. The other Monkman–Grant parameter B^* of open-cell foams, however, depends on their cell structure and the Monkman–Grant parameter of solid cell struts B_s. To compare the theoretical B^* calculated from Eq. (16) with the measured B^* determined from the existing experimental results of aluminum alloy foams with three different relative densities of 0.06, 0.09 and 0.14 [4], the following creep parameters of solid 6101-T6 aluminum alloy are specified: $m_s = 0.88$, $B_s = -1.365$ and $n = 4.4$. As a result, the Monkman–Grant parameters m^* of aluminum alloy foams are all equal to 0.88. At the same time, the other Monkman–Grant parameters B^* of aluminum alloy foams with three different relative densities of 0.06, 0.09 and 0.14 under uniaxial compression can be theoretically calculated from Eq. (16) and are found to be -1.588,

−1.666 and −1.750, respectively. Furthermore, the comparisons between the theoretical results calculated from Eq. (14) and the existing experimental results of aluminum alloy foams under uniaxial compression [4] are shown in Fig. 4. Fig. 4 also shows that the agreements between the existing experimental results and the theoretical results calculated from Eqs. (14)–(16) are good. From a linear regression analysis, it is found that $R^2 = 0.784$ when $\rho^*/\rho_s = 0.06$ using the calculated Monkman–Grant parameters $m^* = 0.88$ and $B^* = -1.588$, $R^2 = 0.893$ when $\rho^*/\rho_s = 0.09$ using $m^* = 0.88$ and $B^* = -1.666$ and $R^2 = 0.794$ when $\rho^*/\rho_s = 0.14$ using $m^* = 0.88$ and $B^* = -1.750$.

By setting $m^* = m_s = 0.88$, the Monkman–Grant parameters B^* can also be determined from the existing experimental results of aluminum alloy foams with three different relative densities of 0.06, 0.09 and 0.14 under uniaxial compression and are found to be −1.930, −1.706 and −1.668, respectively. The comparisons between Eq. (14) using the measured Monkman–Grant parameters B^* and the existing experimental results of aluminum alloy foams under uniaxial compression [4] are shown in Fig. 5. From Fig. 5, it is seen that there is a good agreement between Eq. (14) using the measured Monkman–Grant parameters B^* and the existing experimental results of aluminum alloy foams; $R^2 = 0.967$ when $\rho^*/\rho_s = 0.06$ using the measured Monkman–Grant parameters $m^* = 0.88$ and $B^* = -1.930$; $R^2 = 0.894$ when $\rho^*/\rho_s = 0.09$ using $m^* = 0.88$ and $B^* = -1.706$; and $R^2 = 0.828$ when $\rho^*/\rho_s = 0.14$ using $m^* = 0.88$ and $B^* = -1.668$. It can be said that our theoretical considerations alone cannot prove that failure of open-cell foams behave according to the Monkman–Grant relationship. However, the experimental observation that the foam exhibits a Monkman–Grant behavior with the same exponent as the solid strut material indicates that our assumptions made in theoretical analysis are reasonable.

When $m^* = m_s = 0.88$, the Monkman–Grant parameters B^* of aluminum alloy foams can be either calculated theoretically from Eq. (16) or determined experimentally from the existing experimental results [4], as shown in Figs 4 and 5, respectively. But, the variation of the theoretically calculated Monkman–Grant parameters B^* in Fig. 4 is different from that of the experimentally measured B^* in Fig. 5. Therefore, it can be said that while Eq. (16) overestimates mostly the creep-rupturing time of open-cell foams when their relative density is smaller, it underestimates the creep-rupturing time of open-cell foams when their relative density is relatively higher.

Existing experimental results of the aluminum alloy foam with a relative density of 0.09 under uniaxial compression and uniaxial tension [4] are plotted in Fig. 6. Fig. 6 shows that the existing experimental results of aluminum alloy foams under uniaxial tension are described well by the Monkman–Grant relationship. Also, the creep-rupturing times of open-cell foams under uniaxial tension are similar to those under uniaxial compression. Because

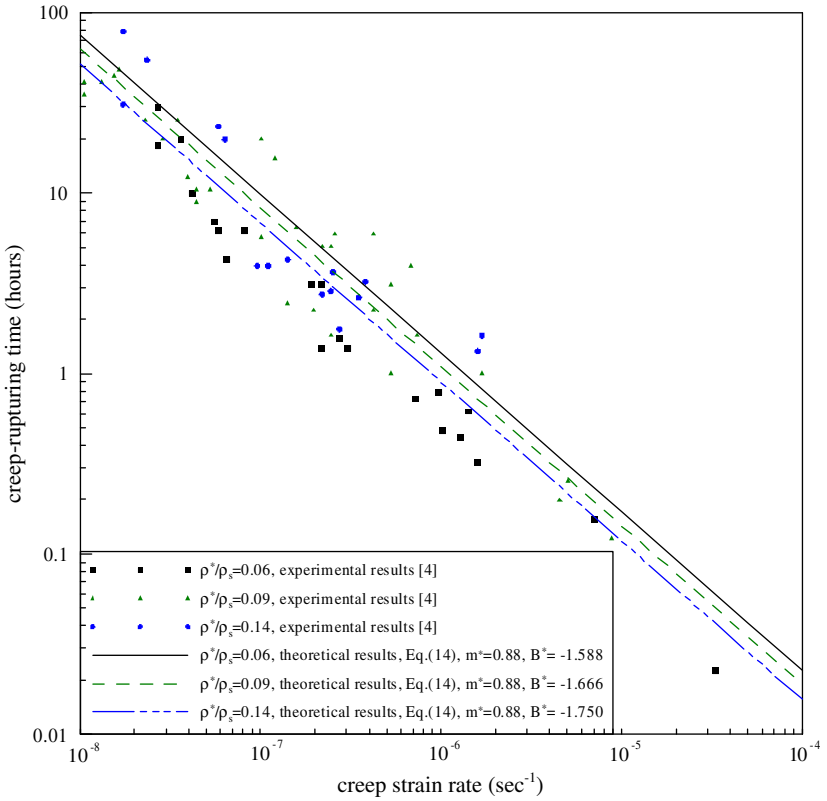

Fig. 4. Comparisons between the theoretical results of Eq. (14) and the experimental results of aluminum alloy foams with three different relative densities of 0.06, 0.09 and 0.14 under uniaxial compression [4].

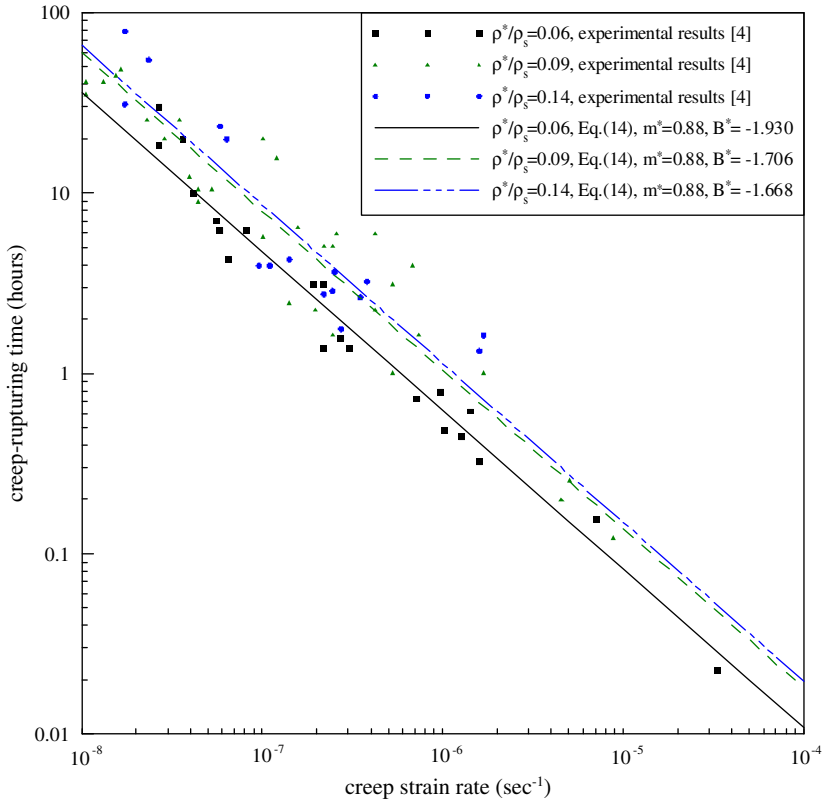

Fig. 5. Comparisons between Eq. (14) using the measured Monkman–Grant parameters B^* and the existing experimental results of aluminum alloy foams under uniaxial compression [4].

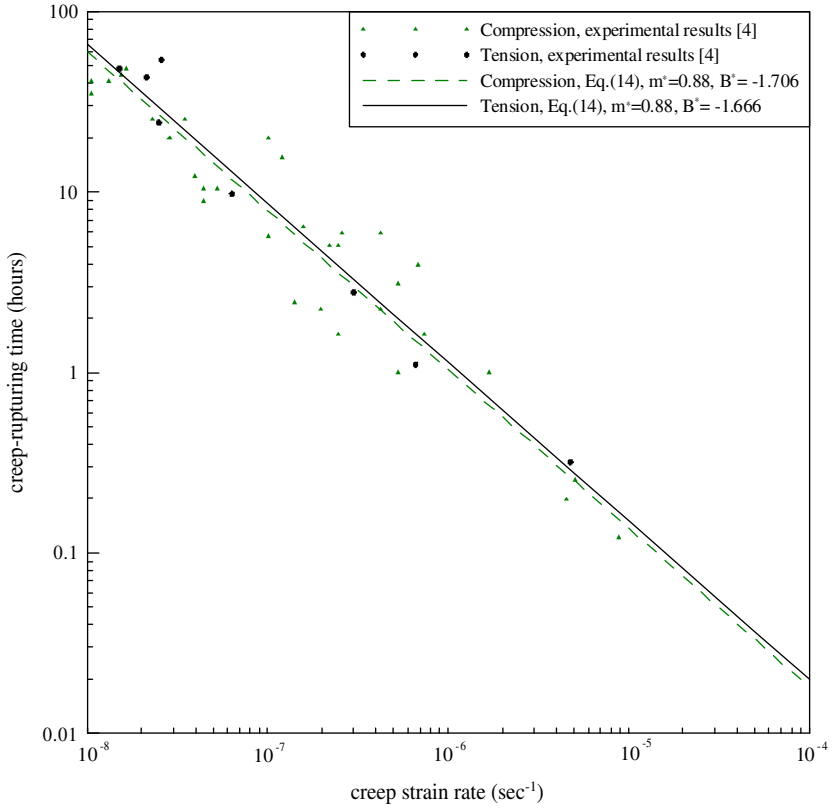

Fig. 6. Comparisons between Eq. (14) using the measured Monkman–Grant parameters B^* and the experimental results of aluminum alloy foams with a relative density of 0.09 under uniaxial compression and uniaxial tension [4].

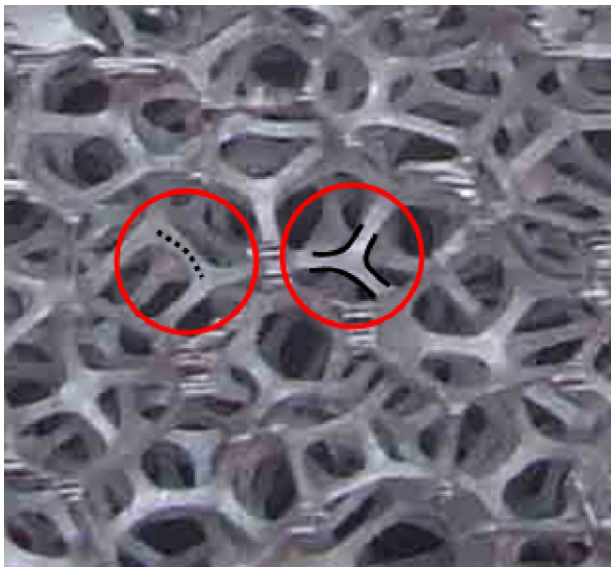

Fig. 7. Cell structural imperfections of variable-thickness cross-section and non-straight profile of cell struts are observed in the aluminum alloy foam with a relative density of 0.06.

cell-strut bending is the dominant deformation mechanism of open-cell foams, it is expected that the theoretical expressions of Eqs. (14)–(16) are valid for both cases of uniaxial compression and uniaxial tension. However, the measured Monkman–Grant parameters B^* determined from the existing experimental results of uniaxial tension and uniaxial compression are different but very close; the measured Monkman–Grant parameter B^* is -1.666 (linear regression $R^2 = 0.970$) for aluminum alloy foams under uniaxial tension.

In practice, there are some pre-existing cell structural imperfections in aluminum alloy foams due to the manufacture process used. The cell structural imperfections of variable-thickness cross-section and non-straight profile of cell struts are commonly observed in aluminum alloy foams as shown in Fig. 7. Moreover, the effects of variable-thickness cross-section and non-straight profile of cell struts on the creep-rupturing of regular hexagonal honeycombs have been analyzed theoretically by Chen and Huang [13]. It is confirmed that the creep-rupturing of regular hexagonal honeycombs is sensitive to their cell structure; the solid distribution in solid cell struts is characterized by Φ_2 while the curvature of cell struts is denoted by ϕ. For example, the Monkman–Grant parameter B^* of regular hexagonal honeycombs with dual imperfections increases slightly at first and then decreases dramatically as Φ_2 is increased while B^* increases gradually as ϕ is increased. Therefore, the difference between the theoretically calculated B^* and the empirically measured B^* of aluminum alloy foams can be attributed to the pre-existing cell structural imperfections of variable-thickness cross-section and non-straight profile of cell struts.

4. Conclusion

The theoretical expressions for describing the steady-state creep strain rate and creep-rupturing time of open-cell foams are derived theoretically. Theoretical results indicate that the steady-state creep strain rate of open-cell foams depends on their cell structure and the creep parameters of solid cell struts. At the same time, it is theoretically verified that the Monkman–Grant relationship is valid for describing the creep-rupturing of open-cell foams. Meanwhile, the Monkman–Grant parameter m^* of open-cell foams is equal to that of solid cell struts m_s. The other Monkman–Grant parameter B^* of open-cell foams, however, depends on that of solid cell struts and their cell structure. By comparing theoretical modeling to existing experimental results, it is found that the variation of the Monkman–Grant parameter B^* calculated theoretically is different from that determined experimentally. Consequently, the creep-rupturing times of lower relative-density aluminum alloy foams are overestimated, while those of higher relative-density aluminum alloy foams are underestimated. The difference between theoretical modeling and experimental results is attributed to the pre-existing cell structural imperfections in aluminum alloy foams. Therefore, the creep-rupturing of aluminum alloy foams is found to be sensitive to their cell structure.

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