Time-dependent creep behavior of Al–SiC functionally graded beams under in-plane thermal loading

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ABSTRACT

Time-dependent steady state creep behavior of functionally graded beams subjected to thermal loading is investigated using a method of successive elastic solution. The functionally graded beams considered in this study are composites of aluminum containing silicon carbide particles with maximum particle contents of 25, 30 and 35 vol.%, but all with an average particle content of 20 vol.%. The steady state creep behavior is described using Norton equation. It is concluded that the steady state creep behavior of the FGMs can be divided into two stages. The first stage involves time dependent stresses followed by the second stage in which the stress remains constant with time. Moreover, both the thermal stress and the curvature of the FGM samples decrease with time during the steady state creep process. This effect is more pronounced for the sample with a higher gradient of particle concentration.

1. Introduction

The severe thermo-mechanical loads involved in many structural components such as those used in nuclear, aircraft, space engineering and pressure vessels require high temperature resistant materials where the conventional materials (metals or ceramics) alone may not survive. Accordingly, attempts have been made to develop superior heat resistant materials. The functionally graded materials (FGMs) as high temperature resistant materials are a new type of advanced composites in which the reinforcing phase varies gradually in one or more direction of the component. Therefore, the problem of interfacial stresses due to thermal mismatch stresses are suppressed while benefitting from the desirable properties of the constituent phases [1,2].

The creep behavior of FGMs at elevated temperatures is crucial and has been analyzed by several authors [3–9]. Singh and Ray [3] analyzed steady-state creep behavior of an isotropic FGM rotating disk. Creep behavior of a rotating functionally graded composite disk operating under a thermal gradient was described by Gupta et al. [4]. You et al. [5] studied creep deformations and stresses in thick-walled functionally graded cylindrical vessels subjected to an internal pressure. They calculated stresses and creep strain rates for different variations of material parameters along the radial direction. Steady state creep deformation of a functionally graded cylinder subjected to internal and external pressures was studied by Chen et al. [6]. Singh and Gupta [7] investigated the effect of anisotropy on the steady state creep of a functionally graded cylinder. Chen et al. [8] by extending the classic beam theory, formulated the bending stress and strain values encountered in a steady state creep of a functionally graded plate under in-plane bending moment. Thermo-elastic creep behavior of a functionally graded rotating disk having a variable thickness was analyzed by Hosseini Kordkheili and Livani [9].

However, in all the above-mentioned studies the time-dependent creep deformation and stress redistribution issues that may occur in the steady state creep of FGMs are ignored. In damage analysis, by considering the deformation histories under time-dependent applied stresses, the creep behavior of structures can be predicted more reliably [10].

Yang [11] investigated the time-dependent creep behavior of hollow cylinder FGMs at steady state creep stage. He used an analytical method and followed Norton’s law to calculate the time-temperature dependence of the stresses. He concluded that after 10 h of creeping, the steady state stress approaches its constant level. Using a semi-analytical numerical method and by employing a method of successive elastic solution, Jafari-Fesharaki et al. [12] worked on the time-dependent steady state creep behavior of a functionally graded hollow sphere under thermo-mechanical loading. They found that under their proposed conditions the required time for reaching the steady state condition is 30 years. Loghman et al. [13] by employing Norton’s law analyzed the time dependent...
steady state creep stress redistribution of thick-walled functionally graded spheres. They demonstrated that based on their time-dependent solution and by considering the history of the involved stresses and strains, the steady-state condition could be approached after 50 years. In another study [14], time dependent thermo-elastic creep response of rotating functionally graded cylinders was studied. We have recently investigated the time dependent creep deformation and stress redistribution in Al–SiC functionally graded beams [15] by considering the effect of different grading patterns on the stress redistribution and deformation in FGMs under pure bending.

Since the FGMs are intended to be used under thermal gradients, the generated thermal stresses play an important role in analyzing their mechanical behavior [16]. However, in much of the work yet conducted on time dependent steady state creep behavior of FGMs, the concept of the involved thermal stresses has been ignored. Moreover, to the best of the authors’ knowledge, any comprehensive study on the time dependent steady state creep behavior of FGM beams considering the thermal gradients, elastic strains as well as thermal strains seems to be lacking. Therefore, the aim of this study is to employ a semi-analytical method for analyzing the time-dependent steady state creep deformation and stress redistribution in Al/SiC functionally graded composite beams subjected to in-plane thermal loading.

2. Properties at different positions of FGM

A FGM beam of infinite length in X direction as shown in Fig. 1 is considered. The beam geometry assumed in this study can be a rectangular cross section of any structural component such as those used in nuclear, aircraft, space engineering and pressure vessels in which the length to height ratio is more than 10. Such applications are frequently associated with design requirements involving extraordinarily severe constraints as to weight and volume and they may be subjected to severe thermal stresses.

The XOZ plane is taken to be the un-deformed bottom plane of the beam and the Y-axis is the vertical coordinate pointing upwards. The distribution of silicon carbide particles C(y) is assumed to change only in the y direction and to increase linearly through the height of the beam as:

\[ C(y) = C_{\text{min}} + \left( \frac{C_{\text{max}} - C_{\text{min}}}{h} \right) y \]  

where \( 0 < y < h \) and \( C_{\text{max}} \) and \( C_{\text{min}} \) are the maximum and minimum volume fractions respectively at the positions \( y = h \) and \( y = 0 \). Moreover, the mechanical and thermal properties of the FGM beams are considered to be dependent on the volume percent of SiC content and vary along its height according to [12]:

\[ N(y) = N_{\text{Al}} + (N_{\text{SiC}} - N_{\text{Al}}) \times \left( C_{\text{min}} + \left( \frac{C_{\text{max}} - C_{\text{min}}}{h} \right) y \right) \]  

where \( N(y) \) is assumed to be a material property of FGM beam such as modulus of elasticity \( E \), thermal expansion coefficient \( \alpha \) or the thermal conduction coefficient \( K \). \( N_{\text{Al}} \) and \( N_{\text{SiC}} \) are the specified properties of pure aluminum and pure silicon carbide respectively.

In this study, the primary state of creep is not considered and we described the steady state creep of the FGM sample by the Norton equation [17] as:

\[ \dot{e}(y) = B(y) \sigma(y) n(y) \]  

where \( \sigma \) and \( \dot{e} \) are respectively the stress and the strain rate values under thermal loading at any point in the Y-coordinate through the height of the beam. \( B(y) \) and \( n(y) \) are the creep parameters for the material at the height \( y \) in the FGM beam. \( B \) and \( n \) values can be calculated based on Pandey’s experiments conducted on Al–SiC composites [18] and formulated by Singh and Ray [19] as:

\[ \log B = -29 + 1729.38 \log e - 274.71 \log T - 1.98 \log P - 15.88 \log C \log n = -21.54 \log e + 3.08 \log T + 0.03 \log P + 0.07 \log C \]  

where \( T \) is the temperature (in Kelvin), \( P \) is the average size of SiC particles (in \( \mu \)m) and \( C \) is the concentration of silicon carbide (in vol.%).

3. Temperature profiles in FGMs

Due to lack of any analytical model, numerical methods have been used for analyzing the thermal conduction in FGMs. Olatunji-Ojo et al. [20] used a finite-element code to analyze thermal conduction in a layered Ni/carbon nano-tube FGM. In another study [21], for thermal conductivity analysis of FGM disks made of Al\(_2\)O\(_3\)/ZrO\(_2\), both the finite difference and finite-element methods were used. Sadowksi et al. [22] investigated the thermal conduction of cylindrical FGM plates by employing alternating direction implicit method. However, in the present study, since we assumed linear distribution of the reinforcing phase as well as one dimensional heat conduction, it is possible to obtain a closed-form solution for the heat conduction equation.

The steady-state one dimensional heat conduction equation is given in Eq. (5) [23]:

\[ \frac{d}{dy} \left( k(y) \frac{dT}{dy} \right) = 0 \]  

where \( T \) is the temperature and \( K \) is the thermal conductivity of the FGM beam which both are considered to depend on the height \( y \).

The homogenous solution of Eq. (5) can be obtained as:

\[ T(y) = \frac{C_1}{(k_{\text{SiC}} - k_{\text{Al}})(\frac{C_{\text{max}} - C_{\text{min}}}{h})} \log \left( k_{\text{Al}} + (K_{\text{SiC}} - K_{\text{Al}}) \times \left( \frac{C_{\text{max}} - C_{\text{min}}}{h} \right) y \right) + C_2 \]  

The constants \( C_1 \) and \( C_2 \) can be determined from the boundary conditions. The steady-state thermal boundary conditions imposed on the top and bottom surfaces may be written as:

\[ T(y)_{|y=0} = T_{\text{min}}, \quad T(y)_{|y=h} = T_{\text{max}} \]  

By substituting the above boundary condition into Eq. (6), the constants \( C_1 \) and \( C_2 \) are obtained as:

\[ C_1 = -\frac{(T_{\text{max}} - T_{\text{min}}) \times (K_{\text{Al}} + (K_{\text{SiC}} - K_{\text{Al}}) \times C_{\text{max}})}{h \times \log \left( k_{\text{Al}} + (K_{\text{SiC}} - K_{\text{Al}}) \times \left( \frac{C_{\text{max}} - C_{\text{min}}}{h} \right) y \right) + C_2} \]  

\[ C_2 = T_{\text{min}} \times \log \left( k_{\text{Al}} + (K_{\text{SiC}} - K_{\text{Al}}) \times C_{\text{min}} \right) \times \left( \frac{C_{\text{max}} - C_{\text{min}}}{h} \right) y \]  

It should be noted that the solution of Eq. (5) for a non-FGM beam can be obtained as:

\[ T(y) = T_{\text{min}} + \left( \frac{T_{\text{max}} - T_{\text{min}}}{h} \right) y \]
In this study the imposed thermal gradient, results in a thermal loading. The temperature is varied in the range of 673 K at the top to 573 K at the bottom surface of the beams. The thermal loading is considered as the difference between the fabrication temperature 550 K, at which the stresses are free, and the temperature profiles for the non-FGM sample with a uniform particle distribution of 20 vol.%, as well as three FGM samples containing silicon carbide particles with maximum particle contents of 25, 30 and 35 vol.%, but all with an average particle content of 20 vol.%. The temperature profiles are shown in Fig. 2. In Fig. 2a the FGM curves are so close that cannot be distinguished from each other. Therefore, we magnified a section of curve (615–625 K vs. 17–22 mm in the Y-coordinate). Now it can be seen from Fig. 2b that for the non-FGM beam Y-coordinate). Now it can be seen from Fig. 2b that for the non-FGM sample with a uniform particle distribution of 20 vol.%, as well as three FGM samples containing silicon carbide particles with maximum particle contents of 25, 30 and 35 vol.%, but all with an average particle content of 20 vol.%. The temperature profiles are shown in Fig. 3. In Fig. 2a the FGM curves are so close that cannot be distinguished from each other. Therefore, we magnified a section of curve (615–625 K vs. 17–22 mm in the Y-coordinate). Now it can be seen from Fig. 2b that for the non-FGM beam Y-coordinate). Now it can be seen from Fig. 2b that for the non-FGM sample with a uniform particle distribution of 20 vol.%, as well as three FGM samples containing silicon carbide particles with maximum particle contents of 25, 30 and 35 vol.%, but all with an average particle content of 20 vol.%. The temperature profiles are shown in Fig. 3. It is evident that the obtained curves are nonlinear. Therefore, we can conclude that the temperature profiles of FGM samples are nonlinear. Furthermore, it is obvious that in FGM beams, increasing the gradient of particle distribution results in more deviation of temperature profile from linearity.

4. Mathematical formulation of creep behavior under thermal loading

A rectangular beam of width \( b \), height \( h \) and infinity length as shown in Fig. 1 in which the temperature varies throughout its height, \( T = T(y) \), is considered. The beam geometry satisfies the plane stress condition and due to its infinity length, no stress concentration is developed on each side of the beam. According to compatibility equations in two-dimensional strain, the compatibility relationship among the strains can be expressed as

\[
\frac{\partial^2 \varepsilon_x}{\partial x^2} + \frac{\partial^2 \varepsilon_y}{\partial y^2} = \frac{\partial^2 \gamma_{xy}}{\partial x \partial y}
\]  

(10)

where \( \varepsilon_x \) and \( \gamma_{xy} \) are assumed to be the sum of elastic, thermal and creep strains.

In the absence of external forces and for in plane stress condition, we have

\[
\sigma_x = \sigma_x(y) \quad \sigma_y = \tau_{xy} = 0
\]  

(11)

Therefore the corresponding strains are:

\[
\varepsilon_x = \frac{\sigma_x}{E} + \alpha \Delta T + \varepsilon_{x0}
\]  

(12)

\[
\varepsilon_y = -\frac{\nu \sigma_x}{E} + \alpha \Delta T + \varepsilon_{y0}
\]  

(13)

\[
\gamma_{xy} = 0
\]  

(14)

where \( \sigma_x \) and \( \sigma_y \) are the stresses in the length and height of the beam respectively; \( \Delta T \) is the thermal loading determined from the heat conduction equation; \( E \) and \( \alpha \) are the modulus of elasticity and coefficient of thermal expansion that are assumed to depend on the variable \( y \) along the height direction; The subscript “c” is used for the creep strain and \( \nu \) is the Poisson’s ratio that is assumed to be constant.

On the other hand, since \( \sigma_x = \sigma_x(y) \) and due to the constant composition in any XOZ plane, i.e., \( C_y = C(y) \), it can be concluded that

\[
\frac{\partial \varepsilon_x}{\partial x} = \frac{\partial^2 \varepsilon_y}{\partial x \partial y} = 0
\]  

(15)

Therefore, substituting \( \gamma_{xy} \) and \( \varepsilon_{xy} \) from Eqs. (14) and (15) into the compatibility Eq. (10), results in:

\[
\frac{\partial^2 \varepsilon_x}{\partial y^2} = 0
\]  

(16)

The corresponding strain is thus:

\[
\varepsilon_x = k(y - y_c)
\]  

(17)

where \( k \) is the curvature and \( y_c \) describes the position of the neutral axis.

Substituting (Eq. (17)) into (Eq. (12)), leads to the following equation:

\[
\frac{\sigma_x}{E} + \alpha \Delta T + \varepsilon_{x0} = k \times (y - y_c)
\]  

(18)

\[
\sigma_x, \alpha, E \text{ and } \Delta T \text{ vary along the height of the sample due to variation of SiC concentration in this direction. Therefore:}
\]

\[
\frac{\sigma_x(y)}{E(y)} + \alpha(y)\Delta T(y) + \varepsilon_{x0}(y) = k \times (y - y_c)
\]  

(19)
Fig. 4. Numerical scheme of computation.
Eq. (19) can be simplified to give:
\[
\sigma_s(y) = E(y) \times [k \times (y - y_c) - \chi(y) \Delta T(y) - \varepsilon_c(y)]
\]
(20)

It should be noted that since the creep strain, \(\varepsilon_c(y)\) is time dependent, subsequently \(\sigma_s(y)\), \(k\) and \(y_c\) will be changed with time due to creep process. Therefore we will have:
\[
\sigma_s(y, t) = E(y) \times [k(t) \times (y - y_c(t)) - \chi(y) \Delta T(y) - \varepsilon_c(y, t)]
\]
(21)
\[
\sigma_s(y, t) \text{ can be determined by knowing the values of } k(t) \text{ and } y_c(t).
\]

The key issue is to find \(k(t)\) and \(y_c(t)\). In the absence of body and external forces and bending moment, the equilibrium equations of FGM beam is expressed as:
\[
\begin{align*}
\sum F_z &= 0 = \int \sigma_z(y, t) \times y \, dA = 0 \\
\sum M &= 0 = \int \sigma_z(y, t) \times y \, dA = 0 
\end{align*}
\]
(22)

By substituting \(\sigma_s(y, t)\) from (Eq. (21)) into (Eq. (22)), the equilibrium equations can be rewritten in the following forms:
\[
\begin{align*}
\sum F_z &= 0 \Rightarrow \int_0^h E(y) \times [k(t) \times (y - y_c(t)) - \chi(y) \Delta T(y) - \varepsilon_c(y, t)] \, b \, dy = 0 \\
\sum M &= 0 \Rightarrow \int_0^h E(y) \times [k(t) \times (y - y_c(t)) - \chi(y) \Delta T(y) - \varepsilon_c(y, t)] \times y \, b \, dy = 0 
\end{align*}
\]
(23)

In order to obtain the history of stresses and strains during creep of FGMs, we can employ Eqs. (21) and (23) in a numerical procedure based on the Mendelson’s method of successive elastic solution [24] which has been proved to yield reliable data on the time dependent steady state creep behavior of FGMs [12,13].

5. Numerical procedure to obtain history of stresses and strains

In the present study we used a numerical procedure based on the method of successive elastic solution. Depending on the involved creep rates, time intervals in the range of \(10^6\)–\(10^{10}\) s, are considered for timing steps. The total time is the sum of time increments as the creep process progresses. The step by step procedure is shown in the flow chart in Fig. 4. In the first cycle of computation \((t = 0)\), initial values of creep strains are considered i.e. \(\varepsilon_c = 0\). Then according to the equilibrium equations (Eq. (23)), \(k\) and \(y_c\) can be obtained on the loading \(\Delta T(y)\). Once the values of \(k\) and \(y_c\) are obtained, the distribution of stress is calculated using Eq. (21). Then the creep strain increments are calculated using the Norton equation. The distribution of stress is then calculated and compared with the initial value for the convergence of the procedure at each time interval.

6. Numerical results and discussion

The following data for geometry and material properties is used to generate results via the present formulation for Al/SiC FGM beams:

\(b = 5\) mm, \(h = 40\) mm, \(K_{Al} = 237\) W m\(^{-1}\) C\(^{-1}\), \(K_{SiC} = 120\) W m\(^{-1}\) C\(^{-1}\),
\(E_{Al} = 70\) GPa, \(E_{SiC} = 410\) GPa, \(\chi_{Al} = 23.1 \times 10^{-6}\) K\(^{-1}\), \(\chi_{SiC} = 4 \times 10^{-6}\) K\(^{-1}\).

The initial stress distribution curves at zero time \((t = 0)\), calculated for the FGM beams with maximum SiC particle contents of 25, 30 and 35 vol.% but all with an average particle content of 20 vol.% are shown in Fig. 5. In this figure the result for a non-FGM beam containing 20 vol.% of particles with a uniform particle distribution is also shown for comparison.

It is observed that in contrast with the homogeneous material that exhibits zero stress along the height of the sample, the imposed thermal gradient results in thermal stress gradients in FGM beams, and this effect is intensified with increased maximum value of particles concentration. This phenomenon can be
explained on the basis of compatibility relationship that exists among the strains. In general, thermal stresses are generated to satisfy the equations of compatibility. In fact, according to Eqs. (16) and (18) it is expected that when, \( \frac{\partial^2 \sigma_{yy}(y)}{\partial y^2} = 0 \) no thermal stresses will be generated. As shown in Fig. 2 the variation of temperature distribution in non-FGM beam is linear. Therefore it can be concluded that there is no need for thermal stresses to satisfy the equation of compatibility. However, in the FGM beams temperature profiles deviate from linearity. As was mentioned earlier, increasing gradient of particle concentration leads to more deviation of temperature profile from linearity. As a result in the samples with higher particle concentration gradients higher stresses are also generated.

Fig. 6 illustrates the development of thermal stress in \( z = h \) for the three FGM samples. It is evident that almost all of the curves exhibit two stages; a sudden decrease in the thermal stress with time followed by almost constant stress values. It can be seen that the steady state creep in the FGM structures derived from Norton’s law (Eq. (3)), is time dependent and comprises two stages. These results can be explained on the basis of the grading structure of FGMs. In these materials, on the basis of equation of compatibility (Eq. (16)), the steady state stress should be redistributed until any part of the structure reaches its constant creep rate which is compatible with other parts of the material. Therefore, any position in the \( Y \) direction (e.g. \( z = h \)) having a specified constant composition requires a specific time for attaining its final stress level. In this study, this period of time is referred as the first stage of steady state creep followed by the condition at which the thermal stress reaches its constant value which is regarded as the second stage of steady state creep.
Fig. 6 also shows that the increased particle concentration gradient results in shortening the time required for attainment of the second stage of steady state creep. For example, this time is about 50 days for the sample containing maximum particle content of 35 vol.% while it is in the order of several hundred years for the other two FGMs. These results can be attributed to the higher thermal stresses generated in the sample with higher particle concentration gradient resulting in higher creep rates. Another feature of the curves illustrated in Fig. 6 is that the induced stress on FGM samples decreases with time during creep. Similar results have also been obtained via a FEM study on creep of FGM microstructures confirming the reduced residual stresses in ceramic–metal joints [25].

In order to gain a better understanding of the two stages of the steady state creep, the variation in the vertical distance of the neutral axis from the lower surface of the sample (y = 0) against time for the FGMs and non-FGM samples are illustrated in Fig. 7. It is observed that at the first stage of the steady state creep this distance increases with a decreasing rate in all the FGM samples until it reaches nearly a constant value corresponding to the second stage of steady state creep. Moreover, as was expected; there is no change in the position of neutral axis in the non-FGM sample.

Fig. 8 presents the variation of the curvature k with time for a non-FGM as well as three FGM samples. It is evident that in all the FGM beams the curvature decreases steeply and non-linearly with time in the first stage until the second stage is reached when the curvature decreases linearly with time. In fact, the linear variation of the curvature happens when the stress level reaches its constant value.

Moreover, as was expected the curvature of the non-FGM sample is constant against time. Also, it is obvious that in comparison with the thermal stresses curves, the effect of creep on the variation of curvature curves with time is not significant. This can be attributed to the relatively high value of the coefficient of thermal expansion of aluminum and its effect on generating high values of thermal strains. Therefore, according to Eq. (21) thermal strains are significantly higher in comparison with elastic and creep strains (structural strains). In fact the total curvature is assumed to be the sum of the thermal and structural curvatures. Thermal curvatures have been calculated on the basis of thermal strains of the top and bottom surfaces of the sample and are obtained as $3.53 \times 10^{-5}$, $6.19 \times 10^{-5}$ and $7.06 \times 10^{-5}$ for the samples with maximum particle contents of 25, 30 and 35 vol.% respectively. Comparing these values with the associated curves of Fig. 8 reveals that more than 90 percent of the total curvature of each sample is due to thermal curvature and this value does not change with time. Therefore, the effect of creep on the variation of total curvature can be ignored. In Fig. 9 the variation of structural curvature against time for the three FGM samples is presented. This figure reveals that in the absence of thermal curvatures, the effect of creep on the structural curvatures is significant. Therefore, for a FGM system with a low value of the coefficient of thermal expansion, the creep process results in decreased curvature with time. This effect may be beneficial in some operating conditions.

To evaluate more clearly the time dependent steady state creep of the FGMs, the rate of variation of thermal stresses, position of neutral axis and curvature are plotted against time for the sample containing maximum particle content of 35 vol.%, as shown in Fig. 10. The time derivatives of the aforementioned curves were obtained by a five-point cubic spline numerical differentiation computer program [26]. It is evident that there is a sudden increase or decrease in the rate of variation of the curves at the beginning of the steady state creep. These initial high transitions can be attributed to the first stage of the steady state creep. Then these values become almost constant determining the second stage of the steady-state creep of the sample. Also it is interesting to note that while the rate of variation of the thermal stress and position of the neutral axis approach the zero value, the rate of variation of curvature approaches a constant value. Therefore, as was mentioned earlier it can be concluded that when the stress level reaches its constant value then the curvature will change linearly with time. Furthermore, as can be seen, the instant of reaching the second stage of the steady state creep is about 50 days for all the three curves. Similar calculations have been done for the two other FGM samples and the shape of the calculated curves were similar to those obtained for the sample containing maximum particle content of 35 vol.%. However, the time required for attainment of their second stage of steady state creep was in the order of several hundred years. It can be concluded that the instant of reaching the second stage for all the thermal stresses, position of neutral axis and curvature curves are identical. Therefore, this stage can be determined on the bases of any of the aforementioned curves.

Fig. 11 shows the stress distribution along Y direction at the second stage of steady state creep for a non-FGM as well as three FGM samples. Comparing Fig. 11 with Fig. 5 reveals that at this stage, the stress distribution is more uniform over the entire height of
the samples in comparison with that of the initial state. Also, it is evident that at the second stage, the stress values are generally lower as compared with those of the initial state. Moreover, it is obvious that the increased gradient of particle concentration results in overall increase in thermal stress over the entire height of the beams. Another feature of the curves depicted in Fig. 11 is that stress is redistributed during the first stage of the steady state creep inside the FGM beams in such a way that the positions with higher particle contents sustain more loads and vise versa.

7. Conclusions

Time dependent steady state creep deformation and stress redistribution analysis of FGM beams under thermal loading is performed using a method of successive elastic solution. The functionally graded beams considered in this study are composites of aluminum containing silicon carbide particles with maximum particle contents of 25, 30 and 35 vol.%, but all with an average particle content of 20 vol.%. The results confirm that the steady state creep behavior of the FGMs can be divided into two stages. In the first stage the level of thermal stresses, the position of neutral axis and the curvature curves are changing steeply and non-linearly with time. In the second stage of the steady state creep thermal stresses and position of neutral axis remain constant while the curvature curves vary linearly with time at a lower rate. Indeed, under the proposed conditions, the second stage of the steady state is reached after about 50 days for the sample containing maximum particle content of 35 vol.% while it is in the order of several hundred years for the other two FGMs. These results are attributed to the higher thermal stresses developed in the sample with a higher concentration gradient as compared with that of the two other FGMs. Moreover, creep of FGM beams under thermal loading results in decreased thermal stresses as well as curvature of the samples with time. This effect is more pronounced for the sample with higher gradient of particle concentration.

Appendix A. Supplementary material

Supplementary data associated with this article can be found, in the online version, at http://dx.doi.org/10.1016/j.commatsci.2016.04.038.

References