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Marina Ćetković[1]

LAYERWISE FINITE ELEMENT FOR THERMAL STABILITY ANALYSIS OF LAMINATED COMPOSITE PLATES

Summary

In this paper thermal stability of laminated composite plates is analyzed using layerwise finite element. The weak form of the plate mathematical model is derived using layerwise displacement field of Reddy [1], nonlinear strain-displacement relations (in von Karman sense) and linear orthotropic thermo-mechanical material properties. The weak form is discretized using isoparametric finite element formulation. The nine-node Lagrangian isoparametric element is used to derive element stiffness and geometric stiffness matrix. The originally coded MATLAB program is used to analyze the effects of different boundary conditions on thermo-elastic stability of laminate composite plate. The accuracy of the numerical model is verified by comparison with the available results from the literature.

Key words

Thermal stability, composite plates, layer wise model, finite element

SLOJEVITI KONAČNI ELEMENT ZA ANALIZU TERMIČKE STABILNOSTI LAMINATNIH KOMPOZITNIM PLOČA

Rezime

U ovom radu analizirana je termička stabilnost laminatnih kompozitnih ploča primenom slojevitog konačnog elementa. Slaba forma matematičkog modela ploče izvedena je koristeći slojevito polje pomeranja koje je predložio Reddy [1], nelinearne veze deformacija i pomeranja (u von Karmanovom smislu) i linearne ortotropne termo-mehaničke karakteristike materijala. Slaba forma je diskretizovana koristeći izoparametarsku formulaciju konačnog elemena. Izoparametarski Lagrangeov konačni element sa devet čvorova je primenjen za izvođenje matrice krutosti elementa i geometrijske matrice krutosti. Originalano napisan MATLAB program je korišćen za analizu uticaja različitih graničnih uslova na termo-elastičnu stabilnost laminatne kompozitne ploče. Tačnost numeričkog modela je potvrđena poređenjem sa raspoloživim rešenjima iz literature.

Ključne reči

Termička stabilnost, kompozitna ploča, slojeviti model, konačni element

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1. INTRODUCTION

In addition to their light weight, high specific strength and stiffness, high damping together with low specific weigh, composite materials posses excellent thermal characteristics. This is the reason why they tend to replace traditional engineering material, such as steel, in thin-walled structures, like nuclear reactors or chemical plants.

It is well known that laminated composite elements, as composed of layers of fibers and matrix material stacked together to form a composite plate or shell element, are not only anisotropic, but also very heterogeneous materials. For such a materials, values of material parameters, such as coefficients of thermal expansion, may not only differ with respect to material used for the constituents, but also with respect to fiber orientation and in some cases with respect to temperature level. This means that fibers and matrix generally exhibit different thermal strains, when exposed to the same amount of temperature. Since they are bonded together and have to satisfy displacement/traction continuity, they develop thermal stresses. At certain level these stresses may decrease the structural stiffness, thus reducing the strength, as well as stability of structural member. Under further increase of temperature, the appearance of instability, or deviation from element initial configuration, may leads to large deflections and eventually complete failure of structural element. This is why thermal stability analysis of composite plates is important criterion for their optimal design.

Generally, mathematical models for thermal buckling analysis of composite laminates have been formulated using three dimensional theory of elasticity (3D), Equivalent Single Layer Theories (ESL), LayerWise Theories (LW) or Zig-Zag theories. Although the 3D theory of elasticity is a powerful tool for exact analyses of laminated composites with severe variations in the material properties, only restricted solutions have been provided by Noor and Burton [2, 3] for thermal buckling problem. The most of the literature regarding thermal buckling problem are based on ESL theories, which are Classical Laminated Plate Theory (CLPT), First-order Shear Deformation Theory (FSDT) and Higher-order Shear Deformation Theory (HSDT).

A thermal buckling solution based on CLPT is firstly given by Jones [4]. Tauchert and Huang [5] used Rayleigh-Ritz technique to analyze the thermal buckling of symmetric angle-ply laminated plates based on CLPT. They analyzed plates subjected to a uniform temperature rise and two types of simply supported boundary conditions.

However, the CLPT neglects transverse shear deformation and therefore becomes inadequate for the analysis of moderately thick to thick laminated composites. In this cases, the shear deformation theory or First-order-Shear-Deformation-Theory (FSDT), which assumes constant transverse shear strains, should be adopted. Tauchert [6] studied the thermal buckling behavior of antisymmetric angle-ply laminates using FSDT. Nath and Shukla [7] used FEM to study the thermal post buckling of laminated plates based on FSDT. The effects of aspect ratio, side to thickness ratio, laminations scheme, boundary conditions, Young's moduli ratio and ratio of coefficients thermal expansion on buckling temperature were examined.

In FSDT, the transverse shear strains are constant through the plate thickness, obtained by the direct constitutive approach and the shear correction factors have to be adopted. However, the shear correction factors are not easy accurately to predict and a higher-order shear deformation theory (HSDT) should be adopted. The HSDT includes higher-order terms in in-plane displacements and may or may not include higher order terms in transverse direction, which is especially important in thermal environment. Sun and Hsu [8] indicated that the transverse shear deformation has a significant effect on the thermal buckling behavior of simply supported plates with symmetric cross-ply lamination. Chang [9] preformed FEM analysis of buckling and thermal buckling of antisymmetric clamped angle-ply laminates subjected to in-plane edge loads or uniform temperature rise.

In all above mentioned equivalent single layer theories (CLPT, FSDT, HSDT) multi-layered composite is modeled as an equivalent single layer homogeneous plate. Such an equivalent single-layer approach may overlook the coupling effect coming from material anisotropy. A layerwise theory (**LW**), which models laminates layer-by-layer, becomes therefore necessary for more accurate thermal buckling analysis of laminated composites. At the authors knowledge, not many papers studied thermal buckling using layerwise concept, except Lee and Shariyat [10, 11] and two papers [12, 13] using local-global or Zig-Zag plate theory. Therefore in this paper LW finite element solution for thermal buckling problem is presented.

After establishing the accuracy of the present layerwise model for linear and geometrically nonlinear bending, vibration and buckling analysis of laminated composite and sandwich plates subjected to mechanical load in the authors previous papers [14, 15] as well as for thermal bending of laminated composite and sandwich plates [16], in this paper a thermal buckling analysis is further investigated. The mathematical model assumes layerwise variation of in-plane displacements and constant transverse displacement through the thickness of the plate, non-linear strain-displacement relations (in von Karman sense) and linear thermo mechanical material properties. The Principle of virtual displacements (PVD) is used to derive the weak form of linearized buckling problem. The weak form is discretized using Lagrangian nine-node isoparametric finite element. The original MATLAB program is coded for finite element solution. The effects of boundary conditions on critical temperature is analyzed. The accuracy of the numerical model is verified by comparison with the available results from the literature.

2. THEORETICAL FORMULATION

2.1 DISPLACEMENT FIELD

In the LW theory of Reddy [1] in-plane displacements components (u,v) are interpolated through the thickness using 1D linear Lagrangian interpolation function $\Phi^I(z)$, while transverse displacement component w is assumed to be constant through the plate thickness.

$$\begin{aligned} u_1(x,y,z) &= u(x,y) + \sum_{I=1}^{N+1} U^I(x,y)\cdot\Phi^I(z) \\ u_2(x,y,z) &= v(x,y) + \sum_{I=1}^{N+1} V^I(x,y)\cdot\Phi^I(z), \\ u_3(x,y,z) &= w(x,y) \end{aligned} \tag{1}$$

2.2 STRAIN-DISPLACEMENT RELATIONS

The non-linear (in von Karman's sense) Green Lagrange strain tensor associated with the displacement field Eq.(1) is computed as:

$$\varepsilon_{xx} = \frac{\partial u_1}{\partial x} + \frac{1}{2}\left(\frac{\partial u_3}{\partial x}\right)^2 = \frac{\partial u}{\partial x} + \sum_{I=1}^{N+1} \frac{\partial U^I}{\partial x}\Phi^I + \frac{1}{2}\left(\frac{\partial w}{\partial x}\right)^2,$$

$$\varepsilon_{yy} = \frac{\partial u_2}{\partial y} + \frac{1}{2}\left(\frac{\partial u_3}{\partial x}\right)^2 = \frac{\partial v}{\partial y} + \sum_{I=1}^{N+1} \frac{\partial V^I}{\partial y}\Phi^I + \frac{1}{2}\left(\frac{\partial w}{\partial y}\right)^2,$$

$$\gamma_{xy} = \frac{\partial u_1}{\partial y} + \frac{\partial u_2}{\partial x} + \frac{\partial u_3}{\partial x}\frac{\partial u_3}{\partial y} = \frac{\partial u}{\partial y} + \frac{\partial v}{\partial x} + \sum_{I=1}^{N+1}\left(\frac{\partial U^I}{\partial y} + \frac{\partial V^I}{\partial x}\right)\Phi^I + \frac{\partial w}{\partial x}\frac{\partial w}{\partial y}, \qquad (2)$$

$$\gamma_{xz} = \frac{\partial u_1}{\partial z} + \frac{\partial u_3}{\partial x} = \sum_{I=1}^{N+1} U^I \frac{d\Phi^I}{dz} + \frac{\partial w}{\partial x},$$

$$\gamma_{yz} = \frac{\partial u_2}{\partial z} + \frac{\partial u_3}{\partial y} = \sum_{I=1}^{N+1} V^I \frac{d\Phi^I}{dz} + \frac{\partial w}{\partial y}.$$

With assumed strain displacement kinematics, an orthotropic linear Hook’s material is assumed for each lamina, to formulate constitutive equations as:

$$\{\boldsymbol{\sigma}\}^{(k)} = [\boldsymbol{Q}]^{(k)} \cdot \left(\{\boldsymbol{\varepsilon}\}^{(k)} - \{\alpha\}^{(k)}\Delta T\right). \qquad (3)$$

where $\boldsymbol{\sigma}^{(k)} = \{\sigma_{xx} \quad \sigma_{yy} \quad \tau_{xy} \quad \tau_{xz} \quad \tau_{yz}\}^{(k)T}$ and $\boldsymbol{\varepsilon}^{(k)} = \{\varepsilon_{xx} \quad \varepsilon_{yy} \quad \gamma_{xy} \quad \gamma_{xz} \quad \gamma_{yz}\}^{(k)T}$ are stress and strain components respectively, $\boldsymbol{Q}_{ij}^{(k)}$ and $\alpha^{(k)} = \{\alpha_{xx} \ \alpha_{yy} \ \alpha_{xy} \ 0 \ 0\}^{(k)T}$ are transformed reduced elastic stiffness [17] and coefficients of thermal expansion of k-th lamina in global coordinates, while ΔT is temperature rise.

2.3 TEMPERATURE RISE

The temperature rise for thermal buckling problem analyzed in this paper, includes uniform temperature rise. The uniform temperature rise assumes that the plate initial temperature is T_i. The temperature is uniformly raised to a final value T_f in which the plate buckles. The temperature change is then $\Delta T = T_f - T_i$.

2.4 EQUILIBRIUM EQUATIONS

The virtual work statement for linearized buckling problem is written using the principle of virtual displacement (PVD):

$$\int_{\Omega} \left\{\{\delta\varepsilon_{0L}\}^T \cdot \{N\} + \{\delta\varepsilon^I\}^T \cdot \{N^I\} + \{\delta\varepsilon_{0NL}\}^T \cdot \{N_T^0\}\right\} d\Omega = 0 \qquad (4)$$

where: $\{N\}=\{N_{xx}\ N_{yy}\ N_{xy}\ Q_x\ Q_y\}^T$, $\{N^I\}=\{N^I_{xx}\ N^I_{yy}\ N^I_{xy}\ Q^I_x\ Q^I_y\}^T$ and $\{N^0_T\}=\{N^0_{Txx}\ N^0_{Tyy}\ N^0_{Txy}\ 0\ 0\}^T$ are the stress resultants in middle and I-th plane, given as:

$$\begin{Bmatrix} N \\ N^I \end{Bmatrix} = \begin{bmatrix} [A] & [B^I] \\ [B^I] & \sum_{J=1}^{N}[D^{JI}] \end{bmatrix} \cdot \begin{Bmatrix} \{\varepsilon_{0L}\} \\ \{\varepsilon^I\} \end{Bmatrix} dz \ , \ \{N^0_T\} = -\sum_{k=1}^{N} \int_{z_k}^{z_{k+1}} [Q]^{(k)} \{\alpha\}^{(k)} \cdot \Delta T \ dz \ . \tag{5}$$

while: $\{\varepsilon_{0L}\}=\left\{\frac{\partial u}{\partial x}\ \frac{\partial v}{\partial y}\ \frac{\partial u}{\partial y}+\frac{\partial v}{\partial x}\ \frac{\partial w}{\partial x}\ \frac{\partial w}{\partial y}\right\}^T$, $\{\varepsilon_{0NL}\}=\left\{\frac{1}{2}\left(\frac{\partial w}{\partial x}\right)^2\ \frac{1}{2}\left(\frac{\partial w}{\partial x}\right)^2\ \frac{\partial w}{\partial x}\frac{\partial w}{\partial y}\ 0\ 0\right\}^T$

and $\{\varepsilon^I\}=\left\{\frac{\partial U^I}{\partial x}\ \frac{\partial V^I}{\partial x}\ \frac{\partial U^I}{\partial y}+\frac{\partial V^I}{\partial x}\ U^I\ V^I\right\}^T$ are strain vector in middle and I-th plane, while $[A]$, $[B^I]$, $[D^{IJ}]$ are given in [17].

3. FINITE ELEMENT MODEL

The governing equations (4) are discretized using GLPT finite element. Over each element, the displacements are expressed as linear combination of shape functions and primary nodal variables as:

$$\begin{Bmatrix} u \\ v \\ w \end{Bmatrix}^e = \begin{Bmatrix} \sum_{j=1}^{m} u_j \Psi_j \\ \sum_{j=1}^{m} v_j \Psi_j \\ \sum_{j=1}^{m} w_j \Psi_j \end{Bmatrix}^e = \sum_{j=1}^{m} [\mathbf{\Psi}_j]^e \{\mathbf{d}_j\}^e , \begin{Bmatrix} U^I \\ V^I \end{Bmatrix}^e = \begin{Bmatrix} \sum_{j=1}^{m} U^I_j \Psi_j \\ \sum_{j=1}^{m} V^I_j \Psi_j \end{Bmatrix}^e = \sum_{j=1}^{m} [\overline{\mathbf{\Psi}}_j]^e \{\mathbf{d}^I_j\}^e \tag{6}$$

where $\{\boldsymbol{d}_j\}^e=\{u^e_j\ v^e_j\ w^e_j\}^T$, $\{\boldsymbol{d}^I_j\}^e=\{U^I_j\ V^I_j\}^T$ are displacement vectors, in the middle plane and I-th plane, respectively, Ψ^e_j are interpolation functions, while $[\boldsymbol{\Psi}_j]^e$, $[\overline{\boldsymbol{\Psi}}_j]^e$ are interpolation function matrix for the j-th node of the element Ω^e , given in [17].

Substituting element displacement field Eq.(6) in to governing equation (4), the following finite element equations are obtained:

$$\left([\boldsymbol{K}]^e - \Delta T_{cr}\,[\boldsymbol{K}_G]^e\right)\{\boldsymbol{\Delta}\}^e = \{\boldsymbol{0}\} \tag{7}$$

where stiffness matrix is given in [17], while element geometric stiffness matrix is:

$$\boldsymbol{K}_G{}^e = \int_{\Omega^e} \begin{bmatrix} [\boldsymbol{G}^e_i]^T [\boldsymbol{N}^0_T][\boldsymbol{G}^e_j] & 0 \\ 0 & 0 \end{bmatrix} d\Omega^e , \tag{8}$$

where: $\left[\boldsymbol{G}_i\right]^e = \begin{bmatrix} 0 \; 0 \; \dfrac{\partial \Psi_i^e}{\partial x} \\ 0 \; 0 \; \dfrac{\partial \Psi_i^e}{\partial y} \end{bmatrix}$, $\left[\boldsymbol{N}_T^0\right] = \begin{bmatrix} N_{Txx}^0 & N_{Txy}^0 \\ N_{Txy}^0 & N_{Tyy}^0 \end{bmatrix}$, $\{\boldsymbol{\Delta}\}^e = \left\{ \begin{matrix} \{\boldsymbol{d}\} \\ \left\{ \sum_{I=1}^{N} \boldsymbol{d}^I \right\} \end{matrix} \right\}^e$. (9)

Solution of equations (7) gives eigenvalues $\Delta T_1, \Delta T_2, \ldots \Delta T_N$. The smallest of the eigenvalues not equal to zero is the critical temperature ΔT_{cr} and the corresponding eigenvector is buckling mode.

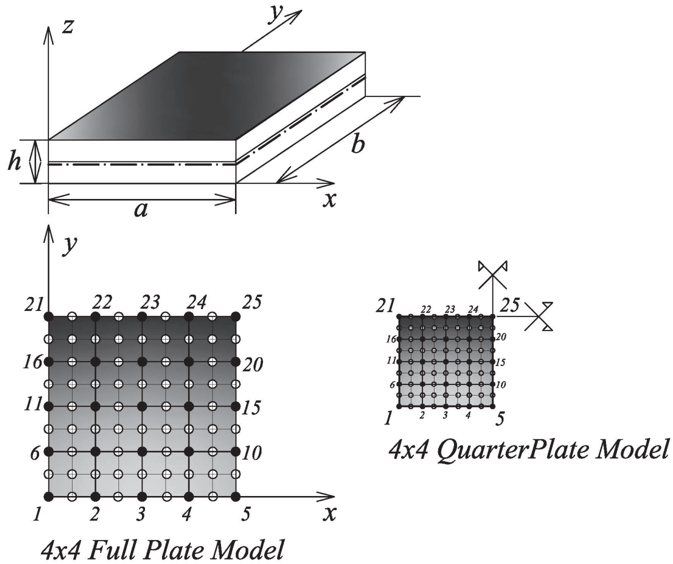

Figure 1. Laminated Plate Configuration and Finite Element Meshes

4. EXAMPLE

Using previous derived finite element solution, an original computer program was coded using MATLAB programming language, for thermal buckling of laminated composite plates. Element stiffness matrix and element geometric stiffness matrix were evaluated using 3x3 Gauss-Legendre integration schemes for 2D quadratic in-plane interpolation. The effect of boundary conditions on critical buckling temperature of laminated composite plates are analyzed using 3x3 and 4x4 full and quarter plate models (Fig. 1). The following boundary conditions at the plate edges are used:

Simply supported:

$$\textbf{SSSS:} \quad \begin{cases} x = 0, a: & u_0 = w_0 = V^I = 0 \\ y = 0, b: & v_0 = w_0 = U^I = 0 \end{cases} \quad I = 1, \ldots N+1 \tag{10}$$

Clamped:

$$\textbf{CCCC:} \quad \begin{cases} x = 0, a: & u_0 = w_0 = U^I = 0 \\ y = 0, b: & v_0 = w_0 = V^I = 0 \end{cases} \quad I = 1, \ldots N + 1 \tag{11}$$

A critical temperature of angle-ply 45/-45 plate subjected to uniform temperature rise is analyzed. Material constants of each layer are:

$$E_1 = 181\ GPa,\ E_1 = 10.3\ GPa,\ G_{12} = G_{13} = 7.17\ GPa,\ G_{23} = 2.39\ GPa,$$
$$\nu_{12} = 0.28, \nu_{13} = \nu_{23} 0.33, \alpha_1 = 0.02\ \alpha_0, \alpha_2 = 22.5\ \alpha_0, \alpha_0 = 10^{-6} 1/{}^0C.$$

The critical temperature is normalized in the following form $\Delta \overline{T}_{cr} = \alpha_0 \Delta T_{cr} \cdot 10^3$. Results of the present model for different boundary conditions are compared with available ones from the literature and are given in Table 1 (a/b=1, a/h=20).

Table 1. Normalized critical temperature for 45/-45 angle-ply laminated composite plate subjected to uniform temperature rise

Boundary conditions	Present				FSDT[7]
	Full Plate Model		Quarter Plate Model		
	3x3	4x4	3x3	4x4	
SSSS	1.029	1.013	0.973	0.963	0.96
CCCC	2.636	2.417	2.220	2.169	2.05
CCCS	1.908	1.787	/	/	1.755
CCSS	1.517	1.418	1.330	1.306	1.517
CSSS	1.173	1.147	/	/	1.18

5. CONCLUSION

In this paper convergence analyses is made for thermal buckling problem of laminated composite plate using layerwise finite element. Finite element solution was incorporated into an original MATLAB computer program, which is used to analyze laminated composite plates with different boundary conditions. Different meshes, coarse or full plate model and fine or quarter plate model, with 3x3 and 4x4 finite elements are analyzed. It is known that as a general rule, finer meshes are required for the buckling problems. As expected, the present results show that the coarse-mesh model predicts higher values of critical temperatures, than fine-mesh model. More ever, for clamped boundary conditions coarse-mesh model gives stiffer response, compared to FSDT[7] model, which may imply that mesh refinement is required. For other type of boundary conditions results of the present model is well predicted with coarse-mesh model and may serve as a benchmark for further investigations.

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