Engineering Notes

An Analytical Model for Predicting Thermal Response in Honeycomb Sandwich Panels

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Nomenclature

T = temperature
X = component or node thickness
k = thermal conductivity
A = area
Q = heat flux
(LO) = radiation excitation flux density
α = absorptance
L = radiant emission
ρ = reflectance

Subscripts

j = node number

Introduction

PROPER design of a load carrying structure subjected to transient heating must be based on a knowledge of the thermal response of the structure. For solid slab structures, whether homogeneous or composite, it is usually reasonable to consider only transient conduction. Many satisfactory computational procedures exist for this purpose. In the case of honeycomb sandwich structures, an accurate prediction of thermal response must consider intracell convection and radiation as well as conduction. A survey of the literature revealed that, prior to this study, no thorough analytical procedure existed for this purpose. To meet the need for such an analysis a study was performed leading to the analytical model described here. This model has been programed for use on the IBM 7090, 7094, and 7044 computers. It was used to predict thermal response of a variety of test specimens and gave results in good agreement with experimental data obtained during the study.

This note summarizes Ref. 1, which gives a detailed description of the analytical investigation and of the computer program. The experimental program is described in detail in Ref. 2.

Analysis

Scope

The analysis applies to honeycomb sandwich panels having either square or hexagonal core cells. Panels may be considered singly or stacked. Stacked panels may be separated by an airgap, by a layer of insulation, or by a skin common to both panels. The exterior face may be uninsulated, or it may be protected by layers of one or two insulating materials. The various physical configurations to which the analysis applies are shown in Fig. 1. The analysis is transient and one-dimensional except for certain conditions when two-dimensional heat flow is considered in one or both skins of the top panel. A variety of heat inputs can be considered at either surface, or one or both surface temperatures may be specified as a function of time. The analysis treats interior heat transfer by conduction, convection, and radiation. It also considers temperature dependent thermophysical properties for the materials of fabrication.

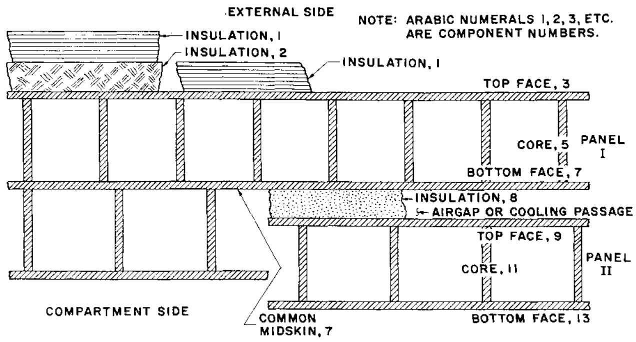

Fig. 1 Physical configurations to which analysis applies.

Method

The analysis is based on an individual idealized core cell as shown in Fig. 2. However, the heat balance equations have been modified to make them applicable to unit area of panel. The heat balance equations are of the finite difference form and employ the forward difference technique for solution. Each node of the idealized core cell shown in Fig. 2 is represented by a heat balance equation. The equations are based on physical reasoning, employing the relationship heat stored/unit time = heat in/unit time − heat out/unit time. A total of 28 heat balance equations are presented in Ref. 1 to permit analysis of each of the configurations shown in Fig. 1.

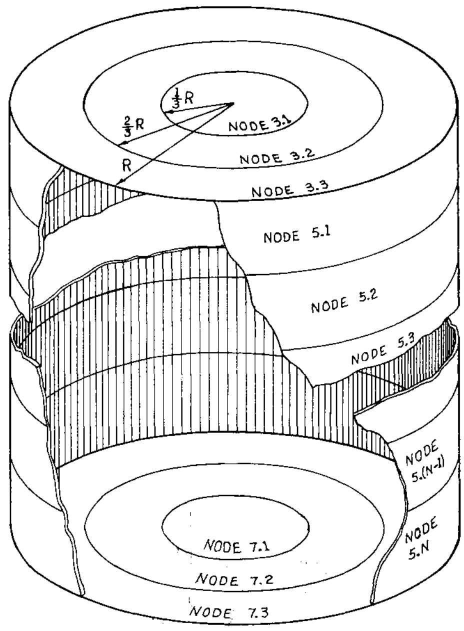

Fig. 2 Idealization of a core cell showing nodal designations.

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Table 1. Description of honeycomb panel test specimens

Cell geometry	Cell diam in.	Core thick-ness, in.	Skin thick-ness, in.	Cell Wall thick-ness, in.	Skin material	Cell material	Mode of joining	No. testd
Square	0.375	0.500	0.01	0.004	15-7PH	17-7PH	Braze	4
Hex	0.500	0.500	0.01	0.004	15-7PH	17-7PH	Braze	4
Hex	0.375	0.750	0.01	0.004	15-7PH	HRP[a]	Bond	3
Hex	2.000	4.000	0.08	0.032	17-7PH	17-7PH	Braze	4

[a] HRP is heat resistant phenolic.

At the surfaces: The heat balance equations applicable to the surfaces consider heat input by convection, radiation, or from an arbitrary source. The convection heat-transfer coefficient must be input as a function of time or temperature. Radiative heat transfer is evaluated between the surface and a source or sink whose temperature may be a specified time dependent function. A constant view factor may be specified. An arbitrary heat flux may be input either as a function of time or of surface temperature, or surface temperature may be specified as a time dependent function. When this latter option is used, the heat flux that produces the specified temperature is computed.

Inside the core cells: The heat balance equations applicable to internal heat-transfer consider conduction, internal convection, and intracell radiation. For each of these considerations, the temperature of each node is evaluated at the midpoint of the node. Conduction between nodes j and $(j + 1)$ is evaluated by the relationship

$$Q_{\text{cond}} = (T_{j+1} - T_j)/[(X/2kA)_j + (X/2kA)_{j+1}]$$

where $X/2$ is the distance between the midpoint of a node and the juncture of the nodes, and k is the thermal conductivity of the node based on the nodal temperature. One-dimensional conduction is considered in the core walls. Two-dimensional conduction is considered in the skins of panel I except when the skin is protected by an insulating layer. When the top face exchanges heat directly with its external environs and/or when the bottom face exchanges heat directly with a compartment or active coolant, the faces are divided into three concentric nodes as shown in Fig. 2. When temperature is specified at one or both faces of panel I, rather than heat flux, an approximate two-dimensional treatment is employed although the face is not subdivided into nodes. This treatment is based on a theoretical effective value of (X/A).

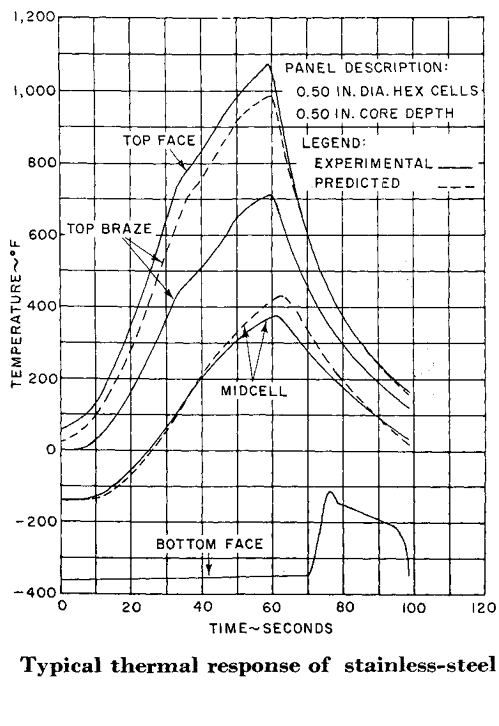

Fig. 3 Typical thermal response of stainless-steel panel.

Intracell radiation is evaluated from a system of simultaneous equations consisting of a separate equation for each node forming the cell enclosure. These equations employ the view factors between each pair of nodes. The system of radiation equations as well as those from which the view factors are evaluated is derived in Ref. 1. The radiation equations are solved by a matrix technique to evaluate the radiant emission from each node. The net radiative heat flux to node j is then evaluated from the relationship

$$(Q_{ir})_j = [(\alpha L)_j - (LO)_j]/\rho_j$$

Internal convection is evaluated from specified time dependent values of film coefficient. The gas temperature in the core cells is assumed to be uniform at any instant in time.

Between stacked panels: When stacked panels share a common midskin, two-dimensional conduction through the midskin is evaluated by a technique derived in Ref. 1. When the panels are separated by an airgap, radiation is considered as between infinite parallel plates, and convection and gas conduction are evaluated jointly by a method presented in Ref. 3. When the space between stacked panels is to be used as a passage for an active coolant, large temperature gradients may occur in the skin of the heated panel. To permit accurate evaluation of both convection and radiation at this skin, an element of skin is divided into three concentric nodes. A technique similar to that employed for evaluating intracell radiation is employed to evaluate radiation across the passage. Convective heat transfer is calculated from specified time dependent values of film coefficient.

Stability: In order to obtain stable solutions to the heat balance equations without an excessive number of time steps, a stability criterion was derived by the technique described in Refs. 4 and 5. This technique is applied to each heat balance equation at each time step to evaluate the time increment dictated by each equation. The smallest increment so obtained is then applied to all the heat balance equations to obtain the change in temperature during the time step.

Evaluation of Analytical Model

Reference 1 (Appendix VI) contains a detailed description of the computer program, including input instructions, flow charts, a description of the pertinent format, and all other information needed to perform an analysis on IBM 7090 or 7094 computers.

The analytical model applied to the digital computer program was used to predict thermal response of several sandwich panels. The results were compared to data obtained from experimental measurements performed on a variety of test specimens. Good agreement was obtained between predicted and experimental results.

Test Program

The experimental program subjected 15 honeycomb sandwich panel specimens to a variety of surface temperature environments. Top face temperatures as high as 1000°F were provided by a bank of tubular quartz infrared lamps. Backface temperatures as low as −300°F were produced by spraying with liquid nitrogen. The panels tested are described in Table 1. Temperatures were recorded at both faces and at other locations inside the specimens while the top face temperature was controlled according to a preselected program. This experimental program is described in detail in Ref. 2.

Comparison of experimental and predicted results

Measured skin temperatures, rather than heat flux, were used as inputs to the computer program to predict thermal response. A typical comparison of experimental and predicted results is shown in Fig. 3. The agreement between measured and predicted midcell temperature is excellent. The poor agreement between measured and predicted top braze temperature is probably a result of faulty braze, hence imperfect contact between the top face and the core wall near the location of the thermocouple used to measure braze temperature. Post test inspection revealed faulty or damaged braze on several panels. For the large cell panels described in Table 1, excellent agreement was obtained between measured and predicted temperature at the top braze.

Good general agreement between experimental and predicted results serves to recommend the analysis for use in design studies.

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Examination of an Aerodynamic Coupling Phenomenon

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Nomenclature

b = wing span, ft
$\bar{c}$ = mean aerodynamic chord, ft
c.g. = center of gravity
C_l = rolling moment coefficient†
C_L = lift coefficient†
C_m = pitching moment coefficient†
C_n = yawing moment coefficient†
g = gravitational constant, ft/sec^2
h = pressure altitude, ft
I_x = roll axis moment of inertia, slug·ft^2
I_y = pitch axis moment of inertia, slug·ft^2
I_z = yaw axis moment of inertia, slug·ft^2
L_α = $\rho SbV_0{}^2\beta_0 C_{l\beta_\alpha}/2I_x$, 1/sec^2
L_β = $\rho SbV_0{}^2 C_{l\beta}/2I_x$, 1/sec^2
L_p = $\rho Sb^2V_0C_{l_p}/4I_x$, 1/sec
L_r = $\rho Sb^2V_0C_{l_r}/4I_x$, 1/sec
m = aircraft mass, slugs
M = Mach number
MAC = mean aerodynamic chord
M_α = $\rho S\bar{c}V_0{}^2C_{m\alpha}/2I_y$, 1/sec^2
M_β = $\rho S\bar{c}V_0{}^2C_{m\beta}/2I_y$, 1/sec^2
M_q = $\rho S\bar{c}^2V_0C_{mq}/4I_y$, 1/sec
N_α = $\rho SbV_0{}^2\beta_0C_{n\beta_\alpha}/2I_z$, 1/sec^2
N_β = $\rho SbV_0{}^2C_{n\beta}/2I_z$, 1/sec^2
N_p = $\rho Sb^2V_0C_{np}/4I_z$, 1/sec
N_r = $\rho Sb^2V_0C_{n_r}/4I_z$, 1/sec
p = rolling rate, rad/sec
q = pitching rate, rad/sec
r = yawing rate, rad/sec
S = wing area, ft^2
T = total engine thrust, lb
V_0 = equilibrium true airspeed, fps
Y_β = $-(T\cos\beta_0/mV_0) + (\rho SV_0C_{y\beta}/2m)$, 1/sec
Z_α = $-(T\cos\alpha_0/mV_0\cos\beta_0) - (\rho SV_0C_{L\alpha}/2m\cos\beta_0)$, 1/sec
α = angle of attack, rad
β = sideslip angle, rad
Δ_{lat} = determinant of the lateral-directional equation's coefficient matrix, 1/sec^4
Δ_{long} = determinant of the longitudinal equation's coefficient matrix, 1/sec^2
λ = differential operator, 1/sec
ρ = atmospheric density, slug/ft^3
ϕ = bank angle, rad

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† Note: Subscripts to these coefficients represent the partial derivative of the coefficients with respect to the subscript variable. Where such subscripts are the angular rates p, q, or r, the derivatives are actually with respect to the conventional nondimensionalized forms of the rates [i.e., $C_{l_p} = \partial C_l/\partial(pb/2V_0)$].

Introduction

AN interesting form of coupling between the longitudinal and lateral-directional rigid body modes of aircraft trimmed at nonzero sideslip angles has been observed and analyzed by the authors. The observed effect of this coupling is a degradation of the dutch roll damping compared to that at zero sideslip trim.

The phenomenon was observed during analog simulation studies of swept-wing transport operations in turbulence, during which the simulated aircraft was flown at exaggerated nonzero sideslip trim conditions. In none of the observed cases did the rate of divergence nor frequency of the motion pose serious control difficulties; in addition, the equilibrium sideslip angles required for pronounced coupling effects were well beyond those that may reasonably be expected for this class of aircraft. Nevertheless, a brief analysis‡ was performed to identify the mechanism of the coupling and to suggest avenues for further research.

Analysis

For this analysis, a set of linearized equations were derived with 5 degrees-of-freedom. Only velocity was assumed constant, thereby retaining all rigid body modes except the phugoid. These linearized equations were derived in the usual manner by restricting the motion to infinitesimal disturbances, or perturbations, from a reference equilibrium state.[1]

A novel feature of the equations developed here is that the chosen equilibrium condition is one with a finite value of sideslip, the wings being held level by appropriate lateral control, necessitating a small equilibrium yawing velocity to provide a quasi-static balance. The equilibrium sideslip could have been balanced by an appropriate bank angle with zero yaw rate, or any proper combination thereof, but the former choice promoted certain computational simplifications. In any event, it is not felt that the exact choice of equilibrium state significantly modifies the ensuing aircraft dynamic characteristics as long as the essential feature, an equilibrium sideslip angle, is permitted. The equations were derived using a principal body axes system for the rotational degrees-of-freedom, and a special wind axes system for the two translational equations.

As the derivation progressed, many terms appeared which serve to couple the longitudinal and the lateral-directional

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