Thermal Stress Analysis of Space Shuttle Orbiter Subjected to Reentry Aerodynamic Heating

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SUMMARY

A structural performance and resizing (SPAR) finite-element computer program and NASA structural analysis (NASTRAN) finite-element computer programs were used in the thermal stress analysis of the space shuttle orbiter subjected to reentry aerodynamic heating. A SPAR structural model was set up for the entire left wing of the orbiter, and NASTRAN structural models were set up for (1) a wing segment located at midspan of the orbiter left wing and (2) a fuselage segment located at midfuselage. The thermal stress distributions in the orbiter structure were obtained and the critical high thermal stress regions were identified. It was found that the thermal stresses induced in the orbiter structure during reentry were relatively low. The thermal stress predictions from the whole wing model were considered to be more accurate than those from the wing segment model because the former accounts for temperature and stress effects throughout the entire wing.

INTRODUCTION

The space shuttle orbiter is designed to be flown as many as 100 missions without excess thermal and mechanical loadings. To establish confidence in the orbiter thermal and structural integrity, it is essential to fully understand both the thermal and structural performance of the orbiter subjected to reentry aerodynamic heating and aerodynamic loading.

Because the number of onboard thermocouples is extremely limited, it is impossible to generate accurate temperature distribution within the orbiter structure based on thermocouple data for estimation of the temperature in each structural component. For this reason analytical thermal analysis (for example, finite-element heat transfer analysis) of the orbiter is necessary. The thermal analysis can give relatively accurate temperature distribution in the orbiter structure, making it possible to determine the temperature level in each of the orbiter structural components. This analysis can show if the design limit temperature of 350°F is exceeded. (Heating beyond 350°F will certainly degrade the aluminum structural material.) Additionally, a thorough knowledge of the structural temperature distribution is necessary for an accurate thermal stress analysis.

The flight load data obtained from onboard strain gage measurements contain both the thermal and mechanical stresses. Unfortunately, these two stress components are not easily separated experimentally. To obtain the mechanical stresses, the thermal stresses must be removed from the strain-gage-measured stresses. This can be done analytically by first calculating the thermal stresses and then removing them from the strain-gage-measured stresses to give the true mechanical stresses. For the thermal stress calculations, the structural temperature distributions obtained from the heat transfer analysis may be used as input to a structural model. Extensive work on the heat transfer analysis of the orbiter was conducted by Ko, Quinn, and Gong (refs. 1 to 7).

The purpose of this report is to use the finite-element method to calculate thermal stresses in the orbiter structure using the structural temperature distributions obtained from references 4 to 7 as thermal loadings to the structural models. Finite-element structural models were set up for the entire left wing, a wing segment
located at midspan of the left wing, and a fuselage segment located at midfuselage. Thermal stress distributions in the orbiter structure were obtained, and the critical high-stress regions were identified. These analyses also provide a baseline for establishing element mesh sizes which will be adequate for thermal stress analysis of large aerospace structures.

NOMENCLATURE

<table>
<thead>
<tr>
<th>Term</th>
<th>Description</th>
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<tr>
<td>CQUAD2</td>
<td>quadrilateral membrane and bending element</td>
</tr>
<tr>
<td>CROD</td>
<td>two-node tension/compression/torsion element</td>
</tr>
<tr>
<td>E23</td>
<td>bar element</td>
</tr>
<tr>
<td>E25</td>
<td>zero-length element for elastically connected geometrically coincident joints</td>
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<tr>
<td>E31</td>
<td>triangular membrane element</td>
</tr>
<tr>
<td>E41</td>
<td>quadrilateral membrane element</td>
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<tr>
<td>E44</td>
<td>quadrilateral shear panel element</td>
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<tr>
<td>FRSI</td>
<td>flexible felt reusable surface insulation</td>
</tr>
<tr>
<td>FS877</td>
<td>fuselage segment structural model at station (X_{0877})</td>
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<tr>
<td>HRSI*</td>
<td>high-temperature reusable surface insulation</td>
</tr>
<tr>
<td>JLOC</td>
<td>joint location (or node)</td>
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<tr>
<td>LRSI</td>
<td>low-temperature reusable surface insulation</td>
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<td>NASTRAN</td>
<td>NASA structural analysis</td>
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<td>RTV</td>
<td>room temperature vulcanized</td>
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<td>SIP</td>
<td>strain isolation pad</td>
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<td>SIP</td>
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<td>STS-5</td>
<td>space transportation system, flight 5</td>
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<td>TPS</td>
<td>thermal protection system</td>
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<td>WING</td>
<td>whole wing structural or thermal model</td>
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<td>WS240</td>
<td>wing segment structural or thermal model at wing station (Y_0) 240</td>
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<tr>
<td>(X_0)</td>
<td>station in x direction</td>
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DESCRIPTION OF PROBLEM

Figure 1 shows a composite top and bottom view of the space shuttle orbiter. The orbiter parts selected for the present study are the major load-carrying regions of the left wing, and the midfuselage cross section (fig. 1). The problem is to calculate thermal stresses induced in the space shuttle orbiter structure during reentry aerodynamic heating. The thermal loadings to the orbiter structure were based on the space transportation system flight 5 (STS-5) reentry trajectory shown in figure 2 (ref. 4). Three regions of the orbiter were selected for the thermal stress analysis. The first region was the whole left wing bounded by chordwise stations X₀1040 and X₀1365. The second region was a segment of the left wing bounded by spanwise stations Y₀-240 and Y₀-254. The third region was a fuselage segment bounded by fuselage stations X₀877 and X₀880. The elevon, leading edge regions, wheel well door, and bay door were not included in the analysis because the present study concerns only the major load-carrying structures. Also the thermal protection system (TPS) was excluded in the thermal stress analysis because it is not a load-carrying structural component. However, for the heat transfer analysis, the wheel well door, the landing gear, the bay door, and the TPS had to be included.

DESCRIPTION OF STRUCTURES

Wing

As shown in figure 3, the ribs of the wing are aluminum truss systems made of vertical, horizontal, and diagonal members pin-joined together. All of the spar webs, the wheel well vertical walls, and the wing root vertical wall (fuselage wall) are made of corrugated aluminum panels. Both the lower and upper wing skins lying between stations X₀1191 and X₀1365 are made of hat-stringer-reinforced aluminum panels. The lower and upper wing skins lying between stations X₀1040 and X₀1191, and the leading edge beam assembly are made of aluminum honeycomb-core sandwich panels. The landing gear door is made of double-walled, hat-stringer-reinforced aluminum panels separated by aluminum stringers. The entire lower wing surface is covered with high-temperature reusable surface insulation (HRSI) tiles, with a
layer of strain isolation pad (SIP) lying between the wing skin and the HRSI for the absorption of strain incompatibility between the aluminum wing skin and the HRSI. Most of the upper skin near the leading edge region is covered with low-temperature reusable surface insulation (LRSI) tiles. A SIP layer lies under the LRSI to function the same as the SIP does for the HRSI. The rest of the upper wing skin, which is subjected to low heating, is covered with highly flexible felt reusable surface insulation (FRSI), under which there is no SIP layer. Room temperature vulcanized (RTV) rubber bonding agent was used in bonding the thermal protection system (TPS) to the wing surfaces. Some of the gaps between the TPS tiles in the high-temperature regions are filled with ceramic-coated aluminum mat (gap fillers) to prevent hot gases from coming in contact with the substructure at the bottom of each gap. Figure 4 shows the structural details of the wing segment of the orbiter right wing.

Fuselage

Figure 5 shows the fuselage cross section located at station X=0877. Both the fuselage bottom (or belly) and the sidewall are made of T-stiffener-reinforced aluminum skins. The lower and the upper glove skins (except for the leading edge region) are made of hat-stringer-reinforced aluminum skins. The leading edge region of the glove skin is an aluminum honeycomb-core sandwich structure. The bay door is a sandwich structure made of honeycomb core and graphite-epoxy composite skins. A small portion of the bay door inner surface is covered with a layer of RTV rubber to serve as a heat sink. The fuselage bottom, lower glove, glove leading edge region, and part of the glove upper surface (near the leading edge region) are covered with HRSI. Most of the upper glove outer surface is covered with LRSI. The lower portion of the sidewall outer surface is covered with FRSI, and the upper portion with LRSI. The upper outer surface of the payload bay door is covered with a layer of FRSI, and lower outer surface with LRSI.

FINITE-ELEMENT MODELING

Because of the complex nature of the space shuttle orbiter structure, some structural simplifications were necessary before setting up the finite-element structural models so that the computations would be manageable. Excessively detailed models could lead to excess computation time, for which the gain in solution accuracies might not be high enough compared with the solutions obtained from simpler yet reasonably detailed models. The previous heat transfer analysis of the orbiter (refs. 1 to 7) showed that representing the hat-stringer-reinforced skins (wing and glove skins), T-stiffener-reinforced fuselage skin, and honeycomb-core sandwich skins with smooth panels of effective thicknesses could give sufficiently accurate temperature solutions. Therefore, in setting up the structural models for the orbiter, a similar approach was adopted.

For the thermal stress analysis of the whole orbiter left wing, a structural performance and resizing (SPAR) finite-element computer program (ref. 8) was used; for the thermal stress analysis of the wing and fuselage segments, a NASA structural analysis (NASTRAN) computer program (ref. 9) was used.
Whole Wing

The SPAR finite-element structural model (WING) setup for the entire orbiter left wing is shown in figure 6. This wing structural model was obtained from direct modification of the whole wing thermal model used earlier for the heat transfer analysis by Ko, Gong, and Quinn (ref. 4). The wing upper and lower skins were modeled with quadrilateral membrane (E41) and triangular membrane (E31) elements. Only two elements were modeled between spar caps, and only one element between the rib caps. The anisotropic material properties were used to account for the effect of the hat-stringers. The fuselage wall (or wing root wall) was modeled with E41 elements. The spar and rib webs and the loading edge beam assembly were modeled with quadrilateral shear panel (E44) elements. The spar and rib caps and the rib truss members were modeled with two-mode bar elements (E23) which have only axial stiffness. The wing root nodes are elastically connected to geometrically coincident points through zero-length E25 elements to simulate the connection of the wing root to the fuselage structure. The size of the entire wing structural model is as follows:

232 JLOCs
498 E23 elements
10 E25 elements
181 E41 elements
19 E31 elements
67 E44 elements

Wing Segment

The NASTRAN finite-element structural model (WS240) for the orbiter wing segment bounded by wing stations YO-240 and YO-254, is shown in figure 7. The upper and lower wing skins, spar webs, and rib cap webs were modeled with quadrilateral membrane and bending elements (CQUAD2). The spar caps and the rib truss members were modeled with two-node tension-compression-torsion elements (CROD).

In order to approximate the actual deformation field of the whole wing, two boundary conditions were used for the WS240 structural model. The YO-254 plane was fixed (no displacement in the y direction), but the displacements in the x and z directions were permitted. For the YO-240 plane, two boundary conditions were used:

1. Plane stress — No rotations with respect to the x, y, and z axes, but free to move in the x, y, and z directions.

2. Plane strain deformation — The y displacements for all the nodes lying in the YO-240 plane were set identically, and rotations with respect to the x, y, and z axes were constrained.

The size of the entire WS240 structural model is as follows:

204 grid points
121 CQUAD2 elements
139 CROD elements
Fuselage Segment

Figure 8 shows the NASTRAN finite-element structural model (FS877) setup for the fuselage segment, bounded by the two planes at fuselage stations X087F and X088O. The bay door was omitted because it is not a major load-carrying structure. The fuselage segment was modeled with CQUAD2 and CROD elements. The X087F plane was fixed (no displacement in the x direction), but the displacements in the y and z directions were permitted. The deformation of the X088O plane was constrained to be plane strain deformation. The entire FS877 structural model has

- 62 grid points
- 89 CQUAD2 elements
- 9 CROD elements

THERMAL LOADINGS

Figure 9 (taken from ref. 4) and figure 10 (taken from ref. 5) respectively show the structural temperature time histories for the wing skins and the fuselage skin. Notice that the structural temperatures for most of the wing skin and fuselage skin stations reached their respective peak values at t = 1700 sec from start of reentry. In this analysis, the structural temperature distributions at t = 1700 sec were used as thermal load input to the structural models for the thermal stress calculations.

For both WS240 and WING structural models, the structural temperature distributions were obtained from the computer outputs of the earlier heat transfer analysis conducted by Ko, Quinn, and Gong (ref. 4). For the FS877 model, the structural temperature distribution was obtained from the computer outputs of the heat transfer analysis done by Ko, Quinn, and Gong (ref. 5). Figure 11 shows the thermal loadings at wing station |Y0| 240 for the WS240 and WING structural models. The difference in the shape of the calculated structural temperature distributions in figure 11 is caused by the number of elements in the two models. Similar distribution can be expected in the thermal stress calculations. Figure 12 shows the thermal loading used for the FS877 structural model.

RESULTS

Figures 13 to 15 respectively show the distributions of the chordwise stress σx, spanwise stress σy, and the shear stress τxy in the orbiter wing lower skin calculated from the WING structural model. The peak compression of both σx (= -9097 lb/in²) and σy (= -2897 lb/in²) occurred near the wing root. The peak tension of σx = 2836 lb/in² and σy = 2405 lb/in² occurred at the leading edge region (figs. 13 and 14). The orbiter wing skin buckling stresses are approximately σx = -12,000 lb/in² and σy = -25,000 lb/in². The peak value of the shear stress |τx| = 7877 lb/in² was located at the wing root trailing edge zone (fig. 15). Figures 16 to 18 respectively show the distributions of σx, σy, and τxy in the orbiter wing upper skin. The peak tension of σx = 3087 lb/in² occurred near the wheel well, and
the peak compression of \( \sigma_x = -2947 \text{ lb/in}^2 \) was located at the wing root trailing edge region (fig. 16). For \( \sigma_y \), the peak tension 2483 lb/in\(^2\) was located at the midspan trailing edge region, and the peak compression \( \sigma_y = -1371 \text{ in/lb}^2 \) occurred at the first bay of midspan (fig. 17).

Like the orbiter wing lower skin, the maximum shear \( |\tau_{xy}| = 2137 \text{ lb/in}^2 \) was located at the wing root trailing edge region. Figure 19 shows the shear stress distribution in the wing spars, leading edge panels, wing root wall, and the elevon support panels. The peak shear occurred at the trailing edge region of the wing root wall (fuselage wall).

Figure 20 shows the axial stresses in the orbiter wing spar caps, wing root rib caps, and other rod elements. The maximum axial tension of 7988 lb/in\(^2\) occurred in the aft wheel well wall lower spar cap; the peak axial compression of -15,408 lb/in\(^2\) occurred at the wing root rib lower cap near the midchord region. Figure 21 gives the axial stresses in the orbiter wing rib truss members. The peak tension of 5412 lb/in\(^2\) occurred at the vertical truss member of the last bay of the rib next to the wing root rib; the peak compression of -9038 lb/in\(^2\) occurred at the lower horizontal truss member of the wing root rib.

Figures 22 to 24 respectively show the chordwise distributions of three thermal stresses \( \sigma_x \), \( \sigma_y \), and \( \tau_{xy} \) at wing cross section \( Y_0-240 \). These stresses were calculated using the WS240 and WING structural models. For the WS240 model, two sets of curves were presented. The first set (solid curves) is for the "plane stress" boundary condition and the second set (dotted curves) is for the "plane strain" deformation. In figure 23, the distribution of \( \sigma_y \) calculated from WS240 model for the wing lower skin and upper skin of bay 1 exhibits stress release zones near the center region of each bay. This implies that the wing skins in those regions have bulged out because of thermal loading. The WING model did not have the capability to show the above stress release effect because of insufficient finite elements. Poor correlation between the predictions based on WS240 and predictions based on WING models could be attributed to:

1. The two sets of boundary conditions used for the WS240 model may not represent the actual deformation conditions.

2. The elements used for the WING model could be too coarse to give more accurate stress distributions.

The variations between the calculated stress data of figures 22 to 24 illustrate how sensitive the wing segment models are to boundary conditions. More importantly, the differences between the calculated stresses from the wing segment model and the wing model are significant in terms of percentage and sign. Even though the wing model is relatively coarse, it is still considered to be the most accurate analysis (of those presented here) because it considers temperature and stress effects throughout the entire wing. The WING model is also believed to be the least susceptible to those boundary conditions imposed in the case of the wing segment model. An increased number of elements in the entire wing model should improve the distribution of calculated stresses between spar caps, but the magnitude and range of stresses would be expected to change very little.
Figure 25 shows the distribution of the axial stress $\sigma_x$ in the fuselage structure. The peak compression of $\sigma_x = -7653$ lb/in$^2$ in the fuselage bottom skin occurred near the vertical wall, and the peak tension of $\sigma_x = 7385$ lb/in$^2$ occurred near the glove leading edge region. Tables 1 and 2 summarize the major peak stresses that occurred in the orbiter major structural components.

Figure 26 shows the deflection curves of the orbiter wing leading and trailing edges. The wingtip deflection induced by the thermal loading is 0.94 in. The inner span region of the wing is twisted in the direction of increasing angle of attack, but the outer span region of the wing is twisted in the opposite direction. Figure 27 shows the spanwise change in angle of attack $\Delta \alpha$ as a result of the thermal loading. The maximum value of $|\Delta \alpha| = 0.1^\circ$ occurred near the wingtip.

Figure 28 shows the deformed shapes of the wing cross section at $x_0-240$ predicted by using the wing segment model under the two boundary conditions mentioned earlier. The entire wing cross section has curved up slightly because of thermal loading. The deformation at bay 1 is most conspicuous for boundary condition 1 (fig. 28(a)), with a vertical displacement of 0.110 in. at the bay 1 lower skin (honeycomb-core sandwich skin). For boundary condition 2 (fig. 28(b)), the peak vertical displacement of 0.121 in. occurred at the bay 1 upper skin. Figure 29 shows the deformed fuselage cross section at $x_0-877$. The thermal loading tended to flatten the fuselage outer bottom, and caused the glove leading edge to move slightly upward, with upward displacement of 0.013 in.

CONCLUSIONS

Thermal stress analyses were performed on the space shuttle orbiter subjected to STS-5 reentry thermal loading. The whole wing, one midspan wing segment, and one midfuselage segment were selected for the analyses. The whole wing was found to be twisted under the thermal loading. The inboard span region of the wing is twisted in the direction of increasing angle of attack, but the outer span region of the wing is twisted in the opposite direction. The maximum twist angle of the wing is about $0.12^\circ$. The correlation between the stress predictions using the whole wing model and the wing segment model was rather poor. The reasons for this may be (1) the boundary conditions used in the wing segment model may not accurately represent the actual deformation field in the whole wing and (2) the whole wing model could be too coarse to give sufficiently accurate stress distributions. Even though the wing model is relatively coarse, it is still considered to be the most accurate analysis compared with the wing segment model because it considers temperature and stress effects throughout the entire wing. The fuselage cross section was found to deform in such a way that the fuselage outer bottom was slightly flattened and the glove leading edge moved slightly upward. Finally, it was found that the thermal stress levels induced in the orbiter structure were relatively low.
REFERENCES


### TABLE 1. - PEAK STRESSES IN ORBITER STRUCTURAL COMPONENTS

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<tr>
<th>Structural component</th>
<th>$\sigma_x$, lb/in²</th>
<th>$\sigma_y$, lb/in²</th>
<th>$\tau_{xy}$, lb/in²</th>
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<td>Wing lower skin</td>
<td>2836</td>
<td>-9097</td>
<td>2405</td>
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<td>Wing upper skin</td>
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<td>-2947</td>
<td>2483</td>
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<td>Wing spar caps and</td>
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<td>533</td>
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<td>Fuselage skin</td>
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<td>-9,038</td>
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### TABLE 2. - PEAK AXIAL STRESSES IN ORBITER SLENDER STRUCTURAL COMPONENTS

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<th>Axial tension, lb/in²</th>
<th>Axial compression, lb/in²</th>
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<tr>
<td>wing root rib caps</td>
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<td>(fig. 20)</td>
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<tr>
<td>Wing rib truss</td>
<td>5412</td>
<td>-9,038</td>
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<td>members (fig. 21)</td>
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Figure 1. Regions of space shuttle orbiter modeled.
Figure 2. STS-5 trajectory (ref. 14).
Figure 3. Space shuttle orbiter wing.

Figure 4. Geometry of wing segment WS240 between wing stations \(Y_0 = 240\) and \(Y_0 = 254\).
Figure 5. Geometry of fuselage cross section at Xg877.
Figure 6. Space shuttle orbiter wing SPAR structural model. TPS, wheel well door, and landing gear excluded.

Figure 7. NASTRAN structural model for orbiter wing segment WS240.
Figure 8. NASTRAN structural model for orbiter fuselage segment FS877.
Figure 9. Orbiter wing skin temperature — time histories at station $Y_0 = 240$. STS-5 flight (ref. 4).
Figure 10. Orbiter fuselage skin temperature — time histories at station X0877. STS-5 flight (ref. 5).
Figure 11. Chordwise distribution of aluminum skin temperatures at wing station $Y_0$ = 240. Time = 1700 sec. STS-5 flight.
Figure 12. Circumferential distribution of fuselage structural temperature at station X0877. STS-5 heating. Time = 1700 sec.
Figure 13. Distribution of normal stress $\sigma_x$ (in lb/in$^2$) in the orbiter wing lower skin. STS-5 thermal loading. Time = 1700 sec.
Figure 14. Distribution of normal stress $\sigma_y$ (in lb/in$^2$) in the orbiter wing lower skin. STS-5 thermal loading. Time = 1700 sec.
Figure 15. Distribution of shear stress $\tau_{xy}$ (in lb/in$^2$) in the orbiter wing lower skin. STS-5 thermal loading. Time = 1700 sec.
Figure 16. Distribution of normal stress $\sigma_x$ (in lb/in$^2$) in the orbiter wing upper skin. STS-5 thermal loading. Time = 1700 sec.
Figure 17. Distribution of normal stress $\sigma_y$ (in lb/in$^2$) in the orbiter wing upper skin. STS-5 thermal loading. Time = 1700 sec.
Figure 18. Distribution of shear stress $\tau_{xy}$ (in lb/in$^2$) in the orbiter wing upper skin. STS-5 thermal loading. Time = 1700 sec.
Figure 19. Distribution of shear stress $\tau_{xy}$ (in lb/in$^2$) in the orbiter wing spars, leading edge panel, wheel well walls, elevon support panels, and wing root wall. STS-5 thermal loading. Time = 1700 sec.

Figure 20. Axial stresses (in lb/in$^2$) in the orbiter wing spar caps, wing root rib caps, and other rod elements. STS-5 thermal loading. Time = 1700 sec.
Figure 21. Axial stresses (in. lb/in²) in the orbiter wing rib truss members. STS-5 thermal loading. Time = 1700 sec.
Figure 22. Chordwise distributions of normal stress $\sigma_x$ in the orbiter wing skins at $Y_0 = 240$, induced by STS-5 thermal loading. Time = 1700 sec.
Figure 23. Chordwise distributions of normal stress \( c_y \) in the orbiter wing skins at \( Y_0 \) 240, induced by STS-5 thermal loading. Time = 1700 sec.
Figure 24. Chordwise distributions of shear stress $\tau_{xy}$ in the orbiter wing skins at $Y_0=240$, induced by STS-5 thermal loading. Time = 1700 sec.
Figure 25. Distribution of axial stress $\sigma_x$ in the orbiter fuselage at station Xo877. STS-5 thermal loading. Time = 1700 sec.
Figure 26. Deflection curves of leading and trailing edges of orbiter wing caused by STS-5 thermal loading. Time = 1700 sec. View looking aft.
Figure 27. Spanwise change in angle of attack of orbiter wing caused by STS-5 thermal loading. Time = 1700 sec.
Figure 28. Deformed shapes of wing cross section at $Y_0 = 240$. STS-5 thermal loading. $T_{max} = 1700$ sec.
Dimensions are in inches.
Figure 29. Deformed shape of fuselage cross section at $x=877$. STS-5 thermal loading. Time = 1700 sec. Dimensions are in inches.
A structural performance and resizing (SPAR) finite-element computer program and NASA structural analysis (NASTRAN) finite-element computer programs were used in the thermal stress analysis of the space shuttle orbiter subjected to reentry aerodynamic heating. A SPAR structural model was set up for the entire left wing of the orbiter, and NASTRAN structural models were set up for (1) a wing segment located at midspan of the orbiter left wing and (2) a fuselage segment located at midfuselage. The thermal stress distributions in the orbiter structure were obtained and the critical high thermal stress regions were identified. It was found that the thermal stresses induced in the orbiter structure during reentry were relatively low. The thermal stress predictions from the whole wing model were considered to be more accurate than those from the wing segment model because the former accounts for temperature and stress effects throughout the entire wing.