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Thin-Film Diffusion Brazing of Titanium Alloys

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A thin-film diffusion-brazing technique for joining titanium alloys by use of a Cu intermediate is described. The manufacturing method employed to fabricate aerospace structures uses integrally heated ceramic platens for the fabrication of flat, tapered, or curved honeycomb panels complete with edgemembers, inserts, and supporting straps.

The method has been characterized in terms of static and dynamic mechanical properties on Ti-6Al-4V alloy. These include tensile, fracture toughness, stress corrosion, shear, corrosion fatigue, mechanical fatigue and acoustic fatigue. Most of the properties of titanium joints formed by thin-film diffusion-brazing are equal or exceed base metal properties. One property, K_{Ic}, is lower for joints, but the threshold stress intensity factor for stress corrosion, K_{Iscc}, is greater for joints than annealed Ti-6Al-4V base metal.

The advantages of thin-film diffusion-brazing over solid state diffusion bonding and brazing with conventional braze alloys are discussed. The producibility advantages of this process over others provide the potential for producing high efficiency joints in structural components of titanium alloys for the minimum cost. Because of its high performance characteristics and relatively low cost, thin-film diffusion-brazing should find extensive use in aerospace vehicles of the future.

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A thin-film diffusion brazing process has been developed for joining titanium structural components such as thin skin honeycomb panels. The process has been termed NOR-Ti-BOND, and it relies on a combination of solid state diffusion and brazing techniques to effect aircraft quality joints in titanium structures.

In this paper, data that characterizes the NOR-Ti-BOND process since its development[1] will be reviewed.

METALLURGICAL PROCESS REQUIREMENTS

The NOR-Ti-BOND process utilizes a thin film of Cu to aid in the diffusion bonding of titanium to itself. When copper is placed in intimate contact with titanium and heated to 1635F, solid state diffusion takes place between the two elements until the eutectic composition, 66% Cu - 34% Ti, is reached according to the equilibrium diagram shown in Figure 1. At this point, a liquid forms and flows like a conventional braze alloy. Continued holding at temperature causes additional diffusion to take place between the liquid and the parent metal so that very quickly the liquid is no longer stable at 1635F and solidification occurs. Further thermal exposure is employed to adjust the final composition of the joint by solid state diffusion to the eutectoid composition of 5.5% Cu + 94.5% Ti. The resulting microstructure, shown in the insert of Figure 1, contains a Widenstatten structure of pro-eutectoid alpha and eutectoid/phases of alpha + Ti_2Cu.

The kinetics of the solid state/liquid state diffusion processes involved at the bonding temperature of 1700F are very rapid. The resulting changes in microstructure and extent of diffusion occurring at the joint interface is shown in Figure 2 for 1, 10, and 60-minute intervals. Within one minute, the joint strength achieves 50% maximum and at 60 minutes, full strength is achieved with a diffused zone of about 0.004 inches.

Copper As An Intermediate

The amount of copper used for joining ranges from 8 to 50 mg/in^2 of the joint area and depends upon the geometry of the joint. For joining honeycomb core or resistance seam bonding of shapes, only 8 mg of Cu/in^2 is used; for honeycomb panels, from 8 to 50 mg of Cu/in^2 of skin area is used, depending upon the core height; for large faying surface joining, properties have been optimized at 50 mg of Cu/in^2 of joint area.

There are several approaches for applying the copper filler material used in the NOR-Ti-BOND process. These approaches include (1) placement of foil, (2) electroplating copper on the edges of the honeycomb core, and (3) electroplating copper on the facesheets and faying surfaces.

Copper foil of the required thickness (0.0002 to 0.0003-inch) is available, but the material is fragile and very difficult to handle dur-

(1) Wells, R. R. and Mikus, E. B., "Thin-Film Diffusion-Brazing of Titanium Members Utilizing Copper Intermediates," U.S. Patent No.3,417,461.

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ing assembly of panel components, particularly where a shearing action is encountered during the assembly operation. However, honeycomb panels have been successfully brazed using copper foil.

With the electroplated honeycomb core edges approach, the Ti-Cu eutectic liquid is formed by reaction with the foil, and severe erosion of the foil may take place. In addition, the determination of the amount of deposit on each edge is difficult.

Electroplating of copper on facesheets is the simplest and most reproducible method for placement. The quantity of copper can be varied and controlled accurately, and it can be easily measured in a nondestructive manner with a beta-ray thickness gage. In addition, the adhesion of the electroplate to the substrate is good so that assembly of panel details is simplified. Erosion is minimized, since the skin or edgemember provides a large diffusion sink for the eutectic reaction.

Liquid Flow

The amount of copper used for joining titanium structures is governed also by the propensity of the liquid Ti-Cu phase to "drain." Since in honeycomb panels both horizontal or vertical planes exist, it is necessary to characterize the eutectic liquid flow characteristics.

The worst possible "draining" condition would occur on a vertical facesheet in an area where a large gap between skin and core exists. In such a case, no capillary forces would be present to direct the liquid into the nodes. To simulate this worst condition, flow tests were conducted on Ti-6Al-4V sheets coated with various amounts of copper and exposed to the braze cycle while oriented in a vertical position. Figure 3 shows that "draining" occurs at copper loading rates of between 40 to 50 mg of Cu/in^2. In addition, the eutectic liquid did not flow beyond the original plated area.

The optimum copper loading rate for various honeycomb core heights is shown in Figure 4. In none of the work performed to date on the fabrication of titanium structure by the NOR-Ti-BOND process has "draining" or eutectic flow been a problem.

NOR-Ti-BOND PROPERTIES

The properties obtainable with the NOR-Ti-BOND process can be classified into two categories; namely, those describing the basic properties of the joint itself and those that describe the properties of a structural configuration.

Joint Properties

Tensile Strength. Basic tensile properties have been determined for Ti-6Al-4V alloy obtained from sheet and 1-inch thick plate and compared with butt joints fabricated using 100 psi pressure, 4 hrs. @ 1700F, and 50 mg Cu/in^2.

After exposure to the fabrication cycle, the plate material retained better than 90% of its original strength; tensile strength decreased approximately 5,000 psi to 138,000 psi, and yield strength decreased approximately 10,000 psi to 124,000 psi, with a corresponding increase in elongation and reduction in area. These mild changes were probably caused by some grain growth during the exposure at 1700F.

Tensile properties of the annealed sheet were similar to those of the plate. Exposure of the sheet to the fabrication cycle decreased the tensile strength approximately 8,500 psi to 132,000 psi and the yield strength approximately 12,000 psi to 125,000 psi, with an increase in elongation and reduction in area. Thus, the effects of the fabrication cycle on tensile properties of the sheet and plate were similar.

Tensile properties of butt joints were determined at temperatures from −150F to 900F, Figure 5. To obtain some measure of the thermal stability of the joints, room temperature tests were also conducted after a thermal exposure of 100 hours at 800F in air. This thermal exposure approximated the same amount of Ti-6Al-4V/Cu diffusion as a thermal exposure of 30,000 hours at 650F, a typical Mach 3 service life. Specimens from both bonding runs exhibited similar tensile strengths. No reduction in properties resulted from this thermal exposure.

Several specimens representing different bonding runs failed in the joints when tested at room temperature and −150F. However, the tensile-strength values were equivalent to strength values for specimens which failed in parent material. In addition, these values were very close to the tensile-strength values obtained for parent material exposed to the fabrication cycle. Visual examination indicated that the joint fractures were relatively flat; whereas, the parent material exhibited the more ductile "cup and cone" type of fracture.

Thus, the tensile strength of a NOR-Ti-BOND butt joint is at least as high as the tensile strength of parent material at temperature in the range of −150F to 900F.

Shear Strength of NOR-Ti-BONDED Joints. Shear strength of lap joints was determined at temperatures from −150F to 900F, Figure 5. With only 1-T overlap joints, all shear type failures were experienced.

The shear strengths of these NOR-Ti-BONDED lap joints are considerably higher than that associated with conventional brazing processes.

Fatigue Properties of Parent Ti-6Al-4V and Butt Joints. Results of sinusoidal, tension-tension fatigue tests at 1200 cpm and a stress-ratio of 0.1 are recorded in Figure 6 for as-received (annealed) Ti-6Al-4V, Ti-6Al-4V exposed to the fabrication cycle and NOR-Ti-BOND butt joints. The data were obtained on coupons taken from three separate bonding runs. No major differences were noted in the fatigue behavior of specimens from different bonding runs.

Of the 14 NOR-Ti-BOND joints tested, ten failed in the parent material and four failed in the joints. One specimen tested at a maximum stress of 89.2 ksi failed in the joint outside the scatter band, and the joint may have been defective. Another, tested at a maximum stress of 103.3 ksi, failed in the joint after 792,000 cycles, which represented the low end of the fatigue scatter band. The remaining two joint failures occurred at maximum stress levels above 120 ksi.

Two of the as-received (annealed) parent material specimens tested above a maximum stress of 120 ksi exhibited better fatigue life than the joints or parent material exposed to the fabrication cycle. This was probably related to the slightly higher strength of the annealed parent material. Below 120 ksi, the fatigue behavior of NOR-Ti-BOND joints, parent material exposed to the fabrication cycle, and annealed parent material fell within the same scatter band, and no significant differences could be detected between the fatigue behavior of joints and parent material. At 10^6 cycles, the fatigue strength was approximately 100 ksi to 115 ksi.

These tests indicate that the fatigue strength of smooth, NOR-Ti-BOND butt joints is excellent and essentially equal to the fatigue strength of parent Ti-6Al-4V, particularly at stresses below 120 ksi. In addition, the results also show that the fabrication cycle does not reduce the fatigue strength of the parent Ti-6Al-4V to any significant degree.

Fracture Toughness. Fracture toughness of NOR-Ti-BOND joints containing 50 mg Cu/in^2 was measured after different fabrication cycles to determine the relationships between processing parameters and joint toughness. All of the specimens were fabricated so that the load was perpendicular to the rolling direction and the crack grew parallel to the rolling direction. The results are shown in Figure 7.

A bonding cycle of one hour at 1700F resulted in a maximum residual copper content of 9.5% at the center of the joint, based upon electron microprobe analysis. Fracture toughness of the joint was comparatively low. Four hours at 1700F resulted in a maximum residual copper content of 7.1%, while specimens held 12 hours at 1700F contained 5.9% and 5.4% copper, respectively. The fracture toughness of the NOR-Ti-BOND joints produced using the chosen processing cycle (4 hours at 1700F) was approximately 60% of the fracture toughness of the as-received material. However, by varying the maximum residual-copper content between 5.4% and 9.5% in the joints, the fracture toughness may range from approximately 40% to 90% of the fracture toughness of as-received (annealed) Ti-6Al-4V.

These results permit us to establish quantitatively the relationships between time at 1700F, maximum residual copper content, and fracture toughness of NOR-Ti-BOND joints.

The fracture toughness of a NOR-Ti-BOND joint does not appear to be highly sensitive to cooling rate. Tests were conducted over a range of cooling rates from 4°/min to 40°/min from 1700F to 1000F.

Fracture toughness tests on specimens exposed to the fabrication cycle show an increase in toughness over the as-received annealed parent material from 75 to 93 ksi $\sqrt{\text{in}}$. This increase was large enough that the specimen thickness of 1-inch no longer met the ASTM recommendation for insuring plane-strain conditions. Thus, the values found are somewhat higher than would result from a thicker specimen, and they are not true K_{Ic} values.

Stress Corrosion Behavior. Tests were conducted to determine threshold stress-intensity factors (K_{Iscc}) for stress corrosion in 3.5% NaCl at room temperature. All of the K_{Iscc} tests were conducted by periodically increasing the load on pre-cracked WOL specimens in small increments until stress-corrosion crack growth occurred when the K_{Iscc} was exceeded. It was observed that stress-intensity factors as little as 10% above K_{Iscc} produced rapid crack growth and specimens failed within one to two hours. These failure times corresponded to crack-growth rates of approximately 1/8-inch to 3/4-inch per hour.

It was found that the K_{Iscc} values for the NOR-Ti-BOND joints are essentially equal to or slightly better than the K_{Iscc} values for as-received (annealed) parent material and for parent material exposed to the fabrication cycle. The ratio K_{Iscc}/K_{Ic} provides a measure of the degree of stress-corrosion susceptibility produced by the salt water. The ratio for the joints is 0.77, which is substantially higher than the values of 0.42 and approximately 0.37 obtained for as-received parent material and parent material exposed to the fabrication cycle. Thus, the parent material is more susceptible to stress corrosion than the joints. In addition, the data show that the fabrication cycle had no major effect on the K_{Iscc} for parent material.

In general, smooth specimens of titanium alloys are not susceptible to stress-corrosion in salt water. Several tests were conducted to determine stress-corrosion susceptibility of NOR-Ti-BOND butt joints in smooth specimens. In addition, one smooth specimen of parent material was tested after exposure to the fabrication cycle. After 1000 hours immersion in 3.5% NaCl at room temperature and under a tensile stress of 102,000 psi, the specimens showed no evidence of corrosion or stress corrosion. The specimens were then tensile tested at room temperature without any reduction in joint properties. Tensile properties were basically the same as those obtained on specimens which were not exposed to a stress corrosion test, Figure 5. Thus, smooth NOR-Ti-BOND joints and parent material exposed to the fabrication cycle are essentially immune to stress corrosion in 3.5% NaCl at room temperature.

Fatigue and Corrosion Fatigue of WOL Specimens. The fatigue behavior of parent material and NOR-Ti-BOND WOL specimens tested in air and in 3.5% NaCl is summarized in Figure 8. The parent material exposed to the fabrication cycle exhibited the largest crack length at failure, followed by annealed parent material and then NOR-Ti-BOND joints when tested in air. The crack lengths at failure were reasonably consistent with the fracture toughness values for the specimens. In 3.5% NaCl, the cracks grew approximately twice as fast as in air for the parent material.

For the NOR-Ti-BOND joints fatigued in air, the crack started in the joint but propagated into parent material and ran parallel to the joint but approximately 1/8-inch from it. This problem was solved by side notches of 1/16-inch radius and 0.025-inch depth along the joint. For a NOR-Ti-BOND joint in 3.5% NaCl, the crack grew approximately three times as rapidly as in air.

On the basis of tests on WOL specimens, the fatigue and corrosion fatigue behavior of parent material was somewhat superior to the behavior of the NOR-Ti-BOND joints. It is noteworthy that fatigue tests in air on smooth butt joints indicated comparable fatigue behavior for joints and parent material. Perhaps the presence of a sharp notch in the WOL specimen has influenced the results.

Several WOL specimens were examined by scanning electron fractography to determine fracture modes in fatigue and corrosion fatigue. As-received (annealed) parent material and parent material exposed to the fabrication cycle exhibited similar fracture modes. Figure 9 shows that the annealed parent material fatigued in air exhibited a normal, transgranular, fatigue failure with a small amount of secondary cracking. The fracture mode under corrosion fatigue, Figure 10, was transgranular quasi-cleavage with some secondary cracking. However, as the crack extended and the stress-intensity factor increased, the failure mode changed to transgranular cleavage with much less secondary cracking.

Under fatigue in air, the NOR-Ti-BOND joint exhibited a normal transgranular fatigue fracture with a small amount of secondary cracking, Figure 11. Under corrosion fatigue, the failure mode, Figure 12, was transgranular quasi-cleavage with secondary cracking along interfaces between alpha needles and eutectoid as well as in the eutectoid. Some areas of transgranular cleavage through the alpha needles were also evident. Thus, the NOR-Ti-BOND joints and parent material exhibited the same basic fracture modes.

It is interesting to note that the corrosion-fatigue failure modes for joints and parent materials were basically the same as failure modes under stress corrosion. Thus, stress corrosion is apparently the dominant part of the failure mechanism operating in corrosion fatigue.

Configuration Controlled Properties

The second category of properties that are used to characterize airframe structures are those influenced by the structural design.

NOR-Ti-BOND Honeycomb Panels. Preliminary design data on honeycomb panels fabricated with the NOR-Ti-BOND process are shown in Figures 13 to 17. The data are limited to flat wall, 1/4-inch square cell, Ti-75A core fabricated by the NOR-Ti-BOND process. Foil gages ranged from 0.0015-inch to 0.003-inch with a density range from 3.4 lb/ft^3 to 6.7 lb/ft^3. Core heights ranged from 0.5-inch to 1.5-inch. A limited amount of test data indicate that these equations are equally valid for Ti-3Al-2.5V alloy core. Ti-6Al-4V alloy skins were employed in thicknesses ranging from 0.012-inch to 0.060-inch.

Acoustical Fatigue Resistance. One of the most effective structural constructions for high acoustic environments is honeycomb panel configurations. This statement is based upon experience gained with adhesively bonded honeycomb panels or brazed metallic panels. In both cases, a considerable fillet is created at the core/skin interface to provide dampening characteristics and lower stress concentrations. Since the NOR-Ti-BOND process relies on a thin-film approach, the fillet formation is limited to only 0.004-inch to 0.005-inch radius. To determine the effect of these small fillets on acoustic fatigue response of NOR-Ti-BONDED panels, a series of beams were run in a progressive wave chamber generating a maximum power level of 167 db.

These beams were 18.5-inches long, 6-inches wide, with a core thickness of 0.5-inch or 0.6-inch. The skins were Ti-6Al-4V alloy. The side exposed to the acoustic pressure was 0.040-inch thick. The other skin was 0.012-inch thick. The core was NOR-Ti-BONDED core made of Ti-75A, non-perforated, flat wall, 1/4-inch square cells, with a foil thickness of 0.003-inch and a core density of 6.7 lb/ft^3.

The beams were open at the sides; i.e., no edgemembers. But at the ends where the support attachment was made, some were sealed with bonded edgemembers.

Each beam was subjected to a series of parallel incidence, progressive wave acoustic exposures. The series consisted of 3-hour exposures at overall sound pressure levels starting at 155 db and increasing by 3 db every 3 hours until failure occurred. Failure was detected by noting changes in the response natural frequency, resonant amplitude, and skin strain as measured by strain gages. The results of these tests are shown in Figure 18.

Beams AB-15 and AB-19 failed in the center of the 0.012-inch skin side at an approximate rms stress of 16 ksi. Beam AB-20 failed at a defective edgemember to 0.040-inch thick skin. Without edgemembers, failure always occurred at the honeycomb-to-skin joint in a peeling mode at lower energy levels. It should be noted that these results, even for the open ended beams, are very acceptable considering the severity of the test.

Thermal Conductivity of Titanium Honeycomb Panels. In airframe design, honeycomb panels are often associated with areas where it is desirable to limit the heat transfer from the aerodynamic boundary layer; i.e., into integral fuel tanks of the wing or interior cabin of a spacecraft.

Titanium has an inherent low thermal conductivity and, when fabricated into honeycomb panels by the NOR-Ti-BOND process, relatively low heat transfer results. If, however, the heat path from the outside skin to the inner skin is increased by the addition of braze alloy in the cell nodes and cell wall/skin interface, a considerable increase in thermal conductivity may result that imposes severe weight penalties due to insulation requirements on the design of the final structure. In Figure 19, the thermal conductivity of a NOR-Ti-BONDED honeycomb panel is compared with a titanium honeycomb panel brazed with an aluminum alloy. The increased conductivity of aluminum plus the increased heat path crosssection of the core has resulted in an order of magnitude rise in the thermal conductivity of the aluminum brazed panel.

MANUFACTURING PROCESSES

Thin-film diffusion brazing by the NOR-Ti-BOND process is a versatile approach to the fabrication of a variety of titanium honeycomb panel configurations. The technique developed utilizes hot ceramic platens as the basic tools.

Fabrication of Honeycomb Panels

Preparation of Panel Materials. Facesheet materials are first trimmed to the approximate size, degreased, and washed. They are lightly cleaned chemically with a nitric-hydrofluoric acid mixture. The sheets are electroplated with copper in a copper sulfate plating bath. Copper thickness on the facesheets and any other plated components is determined with a beta gage. This device uses a beta-ray emitting source and a Geiger counter for determining the thickness of the copper plate in a nondestructive manner.

The honeycomb core comes from two sources; if it is purchased, the foil ribbons are formed and resistance-welded into an expanded honeycomb configuration. This honeycomb must then be rigidized for machining. Machining is performed using a Blanchard grinder to achieve flat surfaces. Contoured surfaces present additional machining or grinding problems. The honeycomb is removed from the rigidizing surface plate, turned over, repotted, and machined on the other surface. Finally, the rigidizer is removed. At this time, the core is inspected for thickness and tolerance; then it is trimmed to the proper shape of the honeycomb panel. If the honeycomb is fabricated using the NOR-Ti-BOND process, it is fabricated and ground in the unexpanded condition. Thus, rigidizers are not required. After machining, the core material is expanded, trimmed to final size, and cleaned.

Edgemember and insert components are normally machined, stress-relieved, and then cleaned in a nitric-hydrofluoric acid solution. In most cases, the surfaces to be brazed are electroplated with copper in a copper sulfate solution. However, in some cases, it is desirable to apply the copper as a foil.

Tolerances used for the various components that are assembled into a honeycomb panel are as follows: facesheet material is normally as-received material from the rolling mills. Expanded honeycomb core is ground to $\pm$ 0.001-inch if it is ground in the unexpanded condition. The height of edgemembers and other inserts must be within ${}^{+\,0.000}_{-\,0.004}$-inch of the honeycomb height in order to achieve the proper tolerance control in the panel.

Tooling and Retorts. The tooling material most commonly used in the fabrication of the titanium honeycomb panels is 321 stainless steel. The surfaces of the stainless steel which contact the titanium are flame-sprayed with zirconium oxide. Titanium surfaces, which were in contact with the zirconium oxide-coated stainless steel, have been examined microstructurally and with the electron microprobe for possible contamination. No contamination has been noted. Stainless steel is used for slip sheets and tooling members to support edgemembers and perform positioning operations. With external Tees or other attachments, machined stainless steel honeycomb is used to fill in large areas rather than massive blocks which create heat sinks.

The panel details are encapsulated in a stainless steel brazing retort made with a stainless steel picture frame covered with thin facesheets used to achieve a diaphragming effect such that a dynamic clamping pressure can be applied to the honeycomb panel. A second matching stainless steel retort (pressure pillow) is used to apply the bonding pressure to the honeycomb panel, forcing good fitup between component parts. The brazing retort and pressure pillow are placed between a fused-silica brazing tool and evacuated using a mechanical roughing pump. Back-filling of the brazing retort is done with ultrahigh-purity argon which is passed through a heated titanium chip-getter. Five purging cycles and a leak check are normally conducted.

Fused-silica tools are heated by resistance wires placed approximately 3/8-inch below the flat surfaces. The surfaces of both tools have a TIR of 0.006-inch across the 2-foot by 3-foot working surface. A system of guard heaters around the sides of the retort has been used to provide good temperature uniformity during the fabrication cycle. Temperature across the panel are held within 50F during heating and within 30F during the one-hour diffusion cycle.

The present technique places the honeycomb panel retort on the bottom and the pressure-pillow retort on the top, Figure 20. During the brazing cycle, the panel to be brazed is pressed against, and tends to duplicate, the surface of the lower brazing tool. Therefore, the aerodynamic surface of the panel is placed down in the retort and the inside or internal structure surface is on the top during the brazing cycle.

For certain applications, the pressure pillow is omitted and the panel is clamped between the two flat silica-tool surfaces. This technique allows very accurate control of the final panel dimensions and good control of the panel configuration. The disadvantages of this system are that a controlled amount of core cell-wall deformation occurs in order to insure complete fit-up between the core and the facesheets. Thus, some portions of the panel have essentially perfect fitup, while others have a honeycomb core which is deformed up to 0.005-inch. In most cases, this amount of cell-wall deformation is irrelevant to the total performance of the structure.

The pressure-pillow technique has the capability of brazing panels without requiring matched tools. One tool controls the configuration of the aerodynamic surface; whereas, the other tool merely backs up the pressure pillow and provides heat for brazing. The pressure pillow then expands and applies the pressure, assures fitup, and presses the panel against the aerodynamic configuration surface.

With proper control of either the matched-tool concept or the pressure-pillow concept, honeycomb panels can be made with little or no deformation of the honeycomb core.

Two techniques are used to apply pressure to the faying surface joints of either edgemembers or inserts during brazing. Both methods require a close-fitting stainless steel tooling bar to be placed within an edgemember or overhanging insert. Since stainless steel expands about twice as much as titanium, the tooling is made to fit at 1600F and is therefore loose at room temperature.

When the edgemember is being brazed with the clamped fused-silica tool technique, the edgemember tooling-bar arrangement acts as a gage block. That is, the silica tools are forced in until they rest on the edgemember. When this happens, the honeycomb core is forced against the facesheet and the necessary compressive load is applied to the faying surface.

The pressure-pillow technique uses a similar concept, except that tooling bars must now be located in the pillow to transmit the force from the silica tool to the brazing retort. These tooling bars are attached to the perimeter of the pillow and not the pillow face, which would impair the diaphraming action.

Brazing Cycle. Present practice of controlling temperature and pressure during a typical brazing cycle follows: the panel-containing retort is purged five times with high-purity, gettered argon and finally backfilled with argon to approximately one-third of an atmosphere. The fused-silica tools are heated at a rate of 500F to 550F per hour to the brazing temperature and held at 1700F.

As the argon within the brazing retort expands on heatup, the internal pressure eventually reaches one atmosphere at 1200F. At this time, the pressure within the retort is allowed to exceed the clamping pressure in order to equalize the argon pressure throughout all of the honeycomb cells. This step insures an even argon pressure and reduces the possibility of having air trapped in a cell which might contaminate the cell prior to brazing. As the temperature increases over 1200F, the argon pressure in the retort exceeds one atmosphere.

With the matched-tool concept, this internal pressure is counteracted by use of the two fused-silica tools pressed against the surface of the retort and panel. Tool pressures are controlled in order to provide a clamping force. The retort edgemembers act like gage blocks in order to prevent excess tool movement from crushing the panel.

With the pressure-pillow technique, the clamping pressure is provided above 1200F by pressurizing the pillow. This pillow then presses against the brazing retort and the fused-silica tools. The fused-silica tool pressure is high enough to overcome the pillow pressure. To prevent collapse of the titanium honeycomb with the pressure-pillow technique, only a small pressure is maintained from 1200F to 1550F. Then, as the brazing or reaction temperature of 1635F is approached, the pressure differential is increased to 1 psi. This clamping force of 1 psi across the honeycomb panel is held until a temperature of 1650F is reached. The pressure is then lowered until, at 1700F, a pressure of 3/8 psi is held to control the configuration of the honeycomb panel. The panel is held at 1700F for one hour to allow the copper to diffuse into the titanium and the titanium to diffuse into the joint.

The cooling rate is controlled by the natural cooling of the silica tools and the honeycomb-panel package. During cooling, a small, positive pressure is maintained against the panel to maintain alignment.

Quality Control and Nondestructive Testing. As previously mentioned, copper thickness is measured nondestructively prior to assembly and brazing. The panels are brazed in an inert atmosphere to assure cleanliness, and the brazing tools assure good fit and dimensional accuracy. The tools are checked for flatness after every run. No change in flatness has been observed after 29 runs, thereby providing good assurance of panel flatness.

After brazing, the panels are checked for flatness and for thickness which provides an indication of quality. Subsequent inspection by X-radiography provides detailed information on the internal structure of the core and the uniformity of node flow. Anomalies such as core-shifting, core-node separation, and cell damage can be detected by this method.

Inspection by ultrasonic pulse-echo and through-transmission C-scanning is conducted with highly reliable results. These techniques are being used primarily to detect facesheet-to-core and facesheet-to-edge-member unbonds.

Indications of unbonds have been verified by destructive tests. The advantage of these X-ray and ultrasonic techniques is that permanent full-size recordings are obtained. Thus, defects can be easily related to their actual location and size in the structure.

SUMMARY

In the case of honeycomb panels, the NOR-Ti-BOND process utilizes only a very small amount of added material, 0.02 lb of Cu/ft^2 of panel, to effect a strong bond equivalent to base metal in almost all properties. The copper, moreover, is placed on the entire sheet, which permits easy electrolytic deposition control of thickness and uniformity. The deposition is strongly adherent and can be quantitatively verified as to the proper quantities prior to bonding.

The core is completely bonded at the nodes. Therefore, the core is easily cleaned and much less susceptible to contaminant entrapment at node points. In addition, the core can be machined to the required height. Typical machining tolerance of $\pm$ 0.003-inch is adequate in an unexpanded condition. This feature is desirable for low core preparation costs and easy handling qualities.

Another important feature of the NOR-Ti-BOND process is the option to bond "in situ" panel inserts, edgemembers, and straps while the panel is being bonded. This feature precludes the use of subsequent fabrication processes to obtain a complete panel. Honeycomb panels, moreover, can be made to any desired shape with the appropriate tooling and fabrication process.

The producibility advantages, coupled with high performance characteristics at relatively low cost, makes the thin-film diffusion brazing of titanium process a leading contender for the fabrication of aerospace vehicles of the future.

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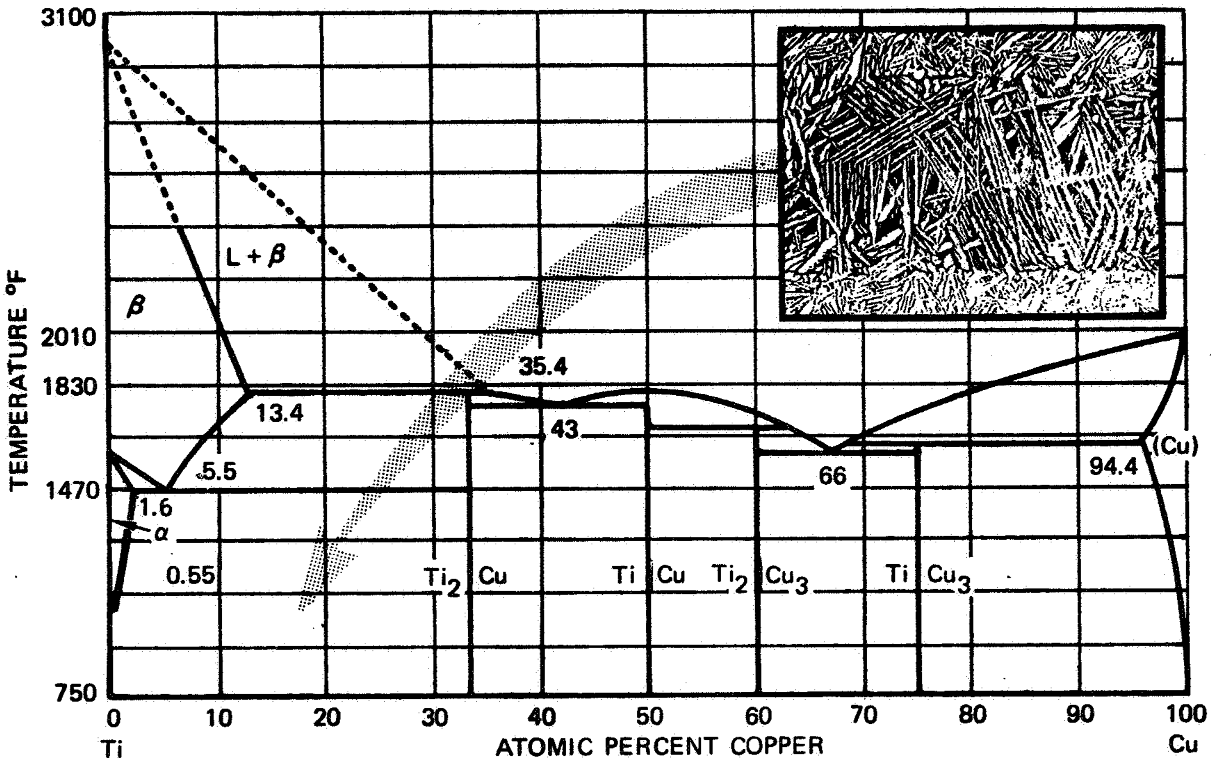
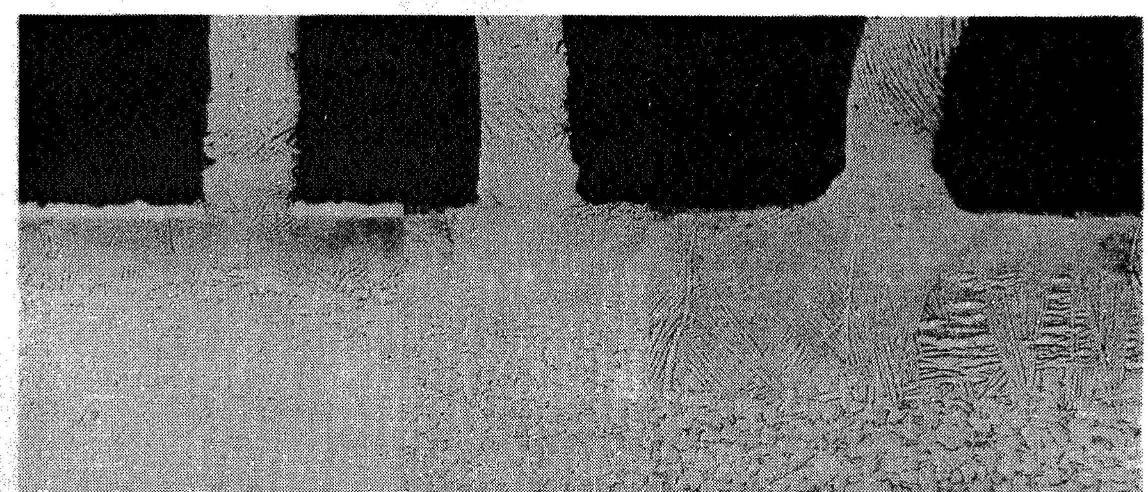
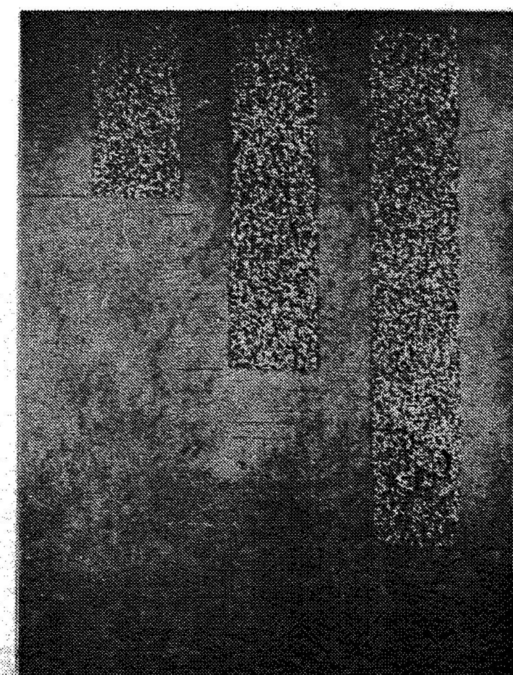


FIGURE 1. Ti/Cu PHASE DIAGRAM

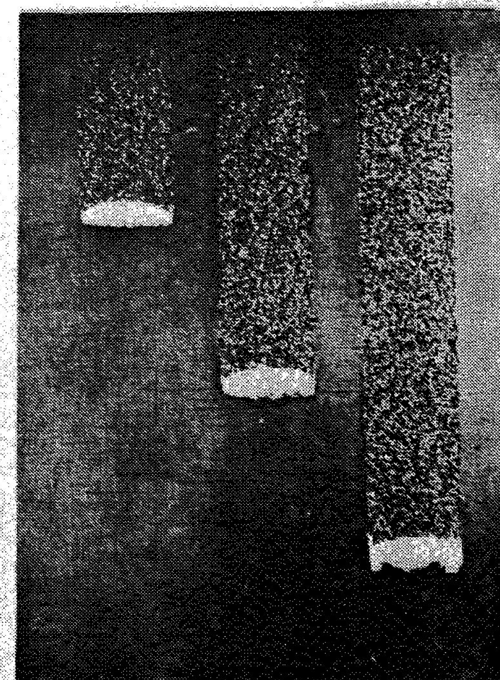


1 MIN 10 MIN 60 MIN

FIGURE 2. DIFFUSION OF Cu IN Ti AT JOINT INTERFACE DURING BONDING AT 1700°F



COPPER QUANTITY: 40 MG/IN^2
NO "DRAINING"



COPPER QUANTITY: 50 MG/IN^2
"DRAINING" OCCURRED

HEATING RATE: 400F/HR

FIGURE 3. VERTICAL DRAIN BEHAVIOR OF NOR-Ti-BOND LIQUID ON Ti-6Al-4V

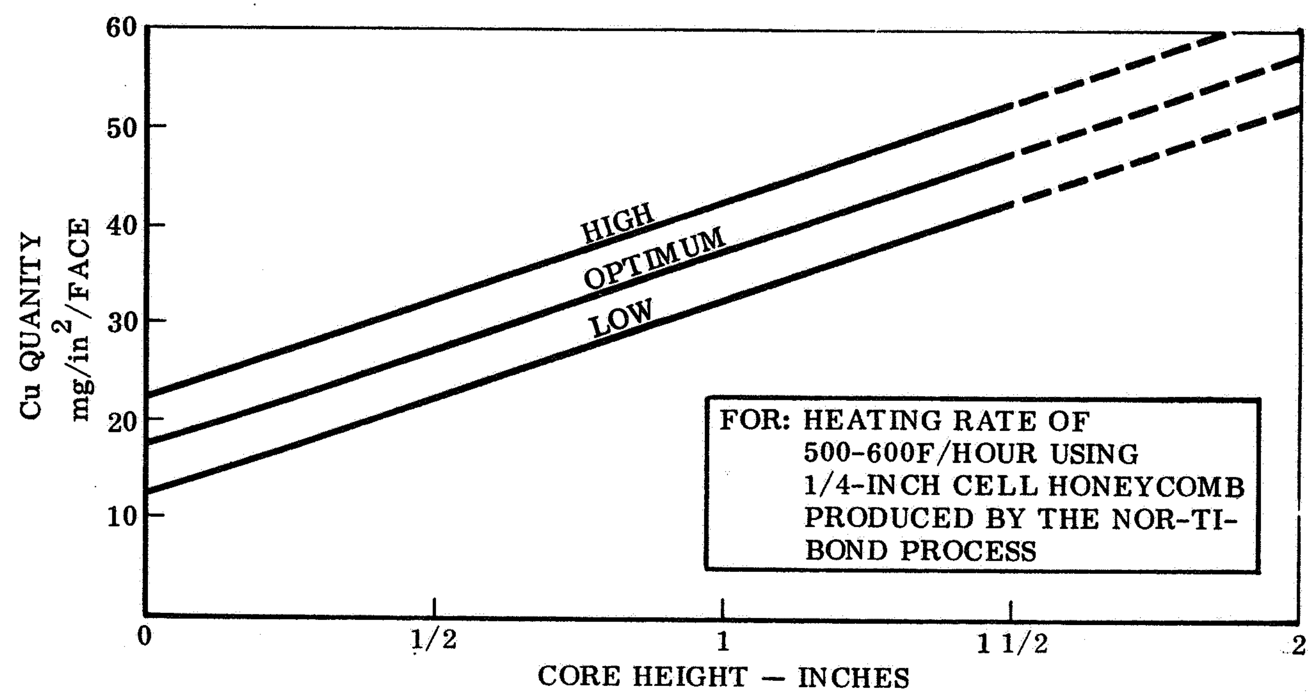


FIGURE 4. COPPER QUANTITY REQUIRED TO FABRICATE TITANIUM HONEYCOMB PANELS

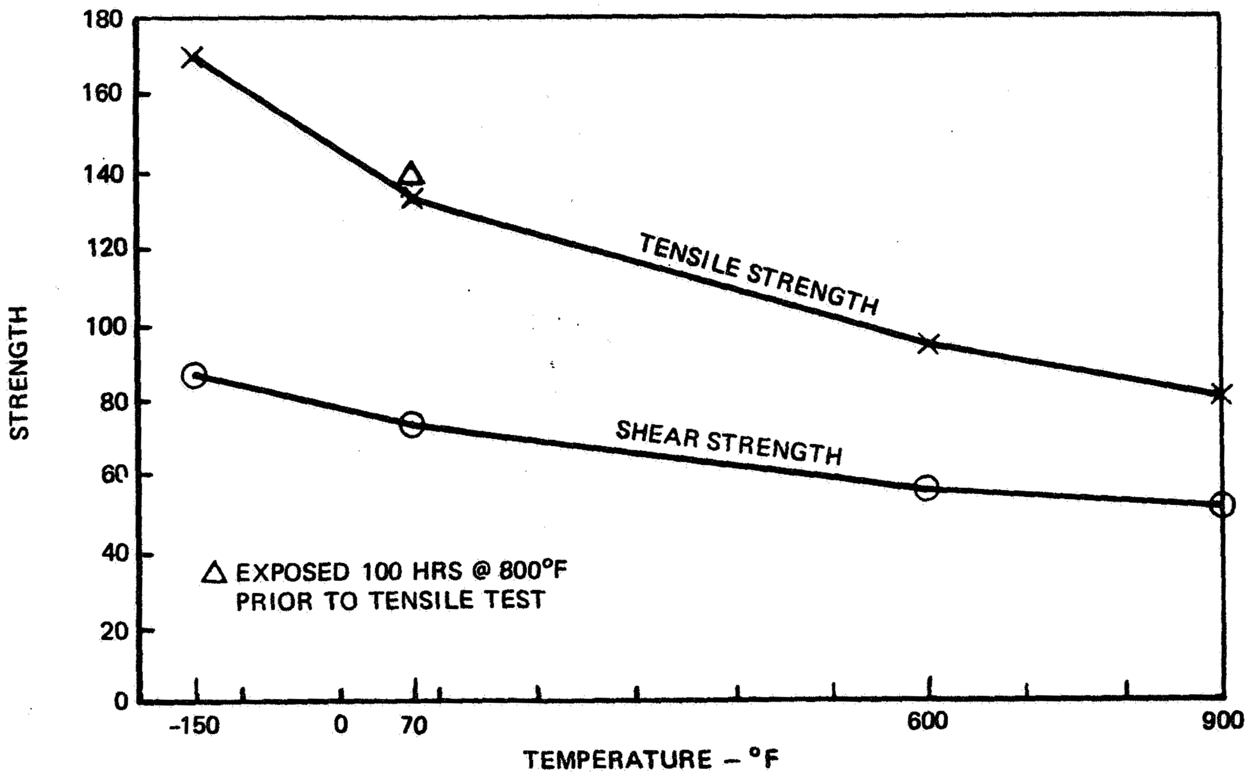


FIGURE 5. TENSILE AND SHEAR PROPERTIES OF NOR-Ti-BONDED JOINTS

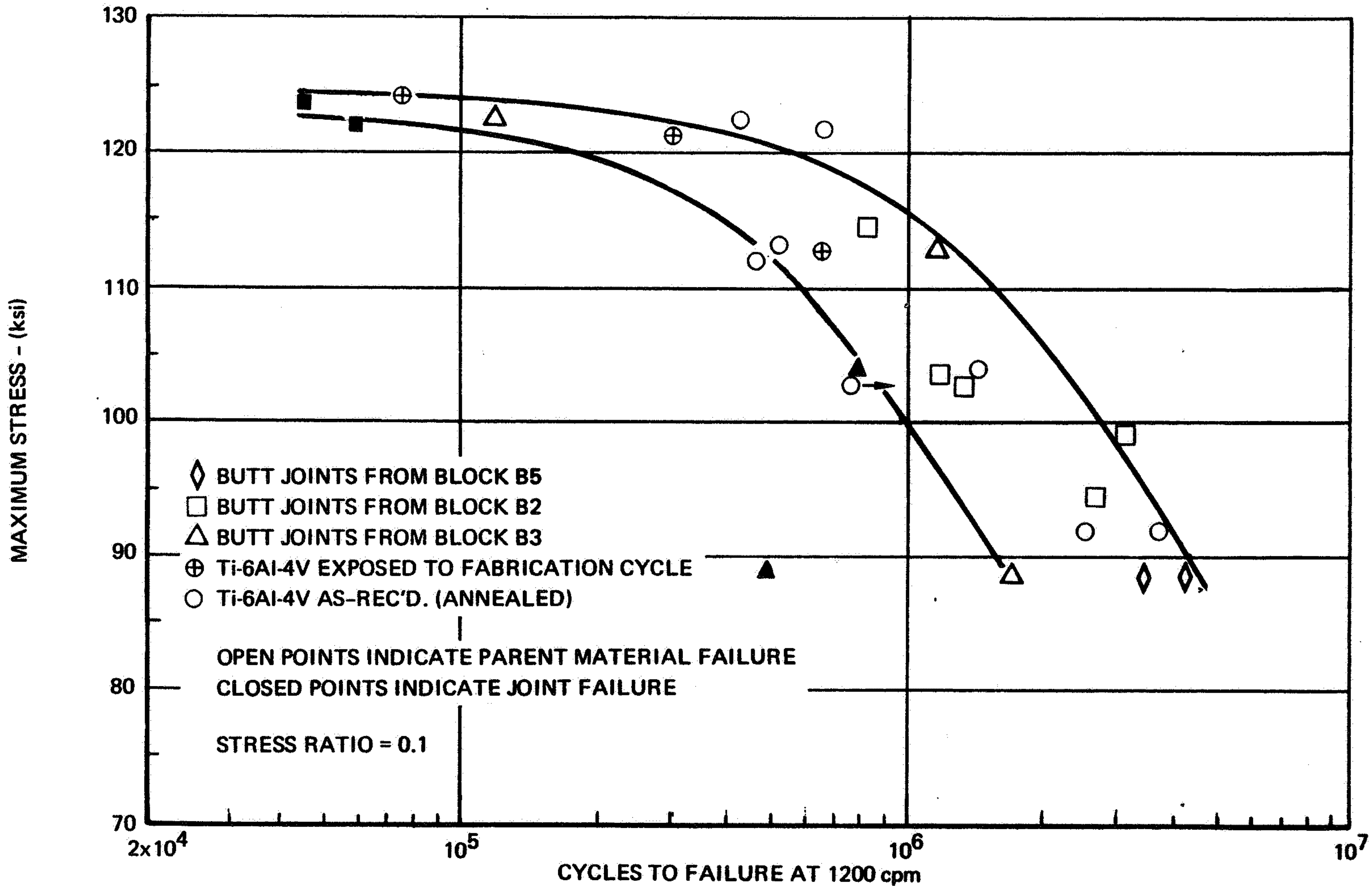


FIGURE 6. SINUSOIDAL, TENSION–TENSION FATIGUE PROPERTIES OF PARENT Ti-6Al-4V AND NOR-Ti-BOND BUTT JOINTS

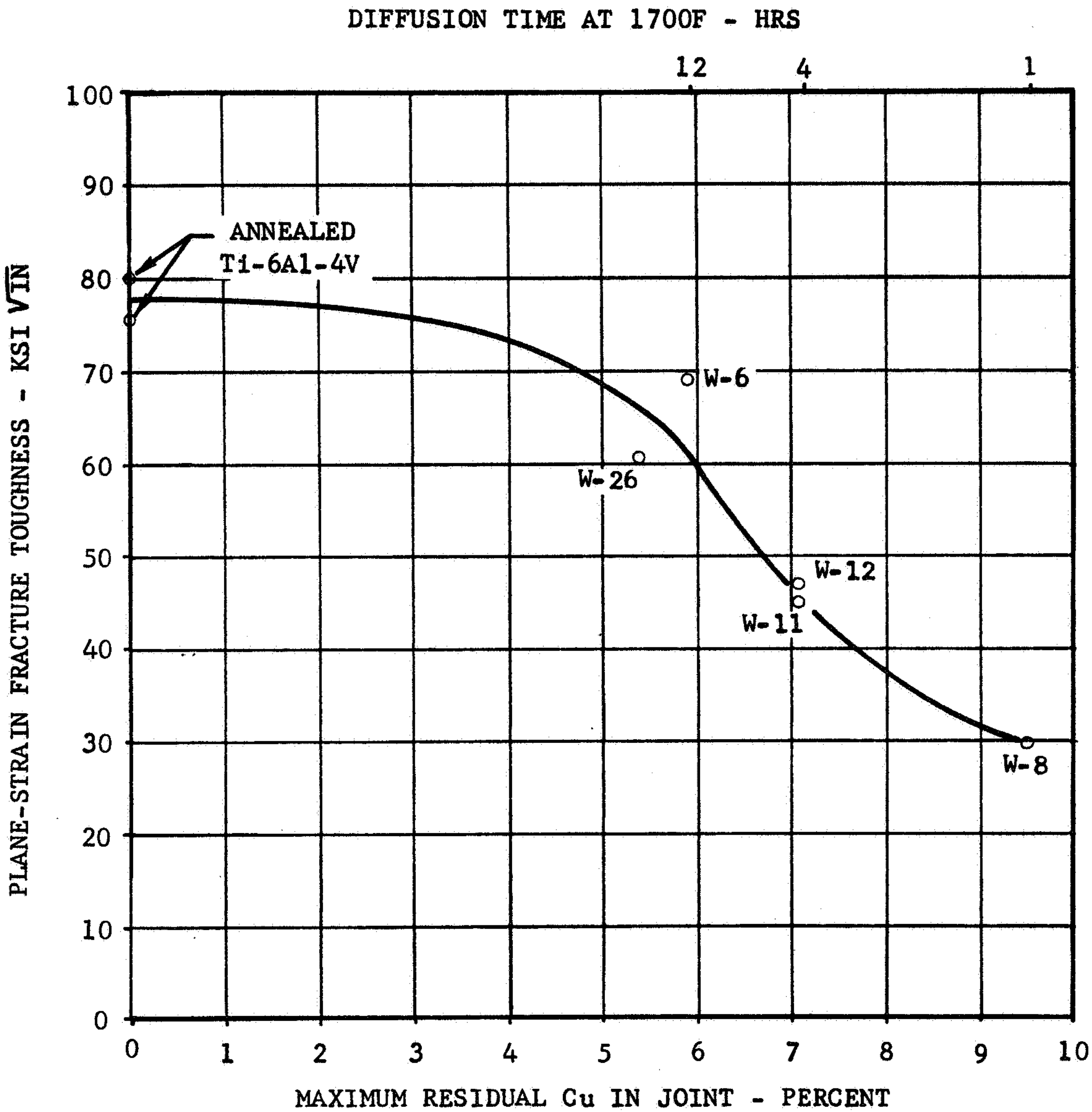


FIGURE 7. EFFECT OF RESIDUAL COPPER CONTENT AND DIFFUSION TIME AT 1700F (FOR 50 mg/in^2 Cu) ON FRACTURE TOUGHNESS OF NOR-Ti-BOND JOINTS

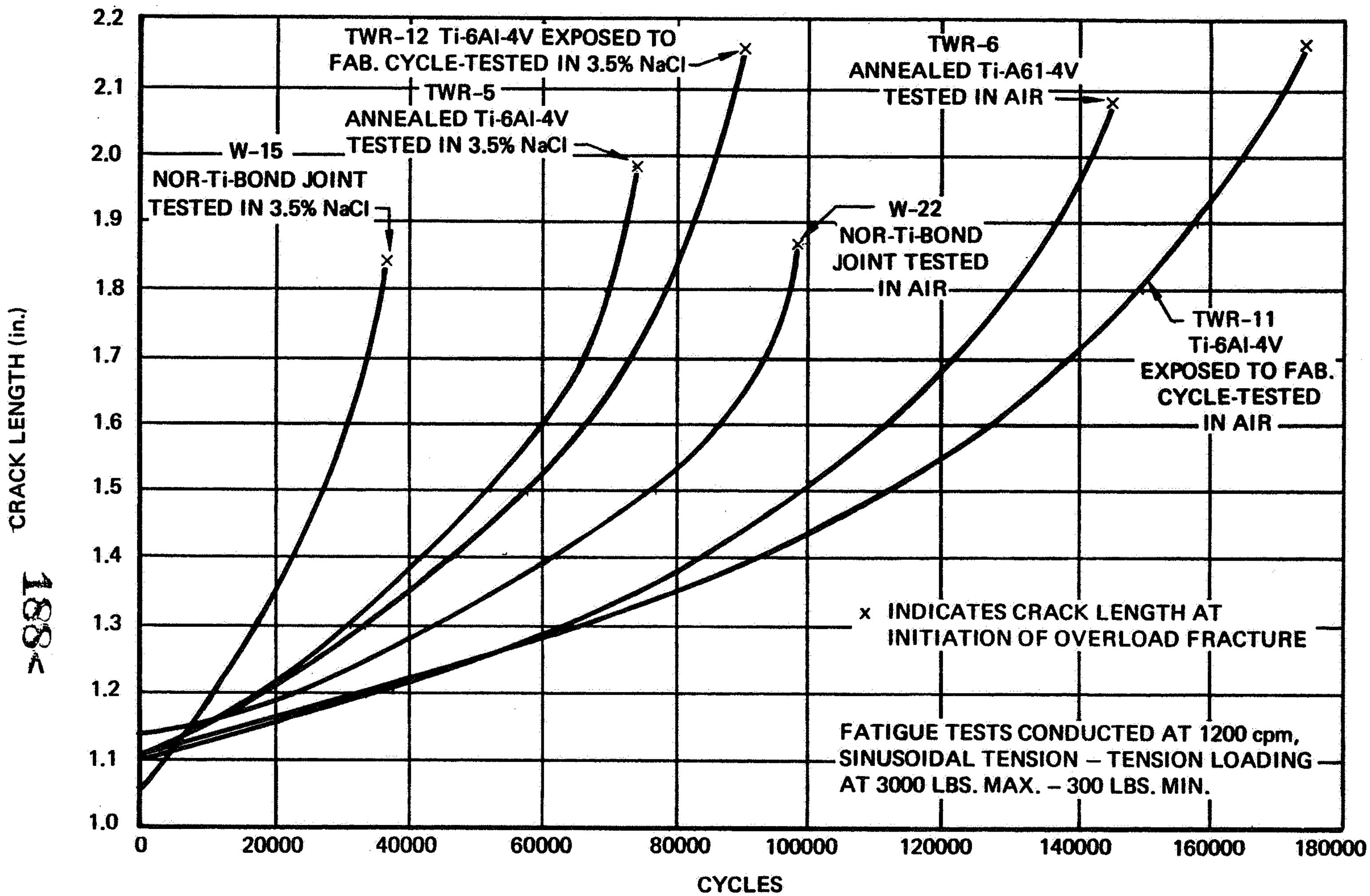
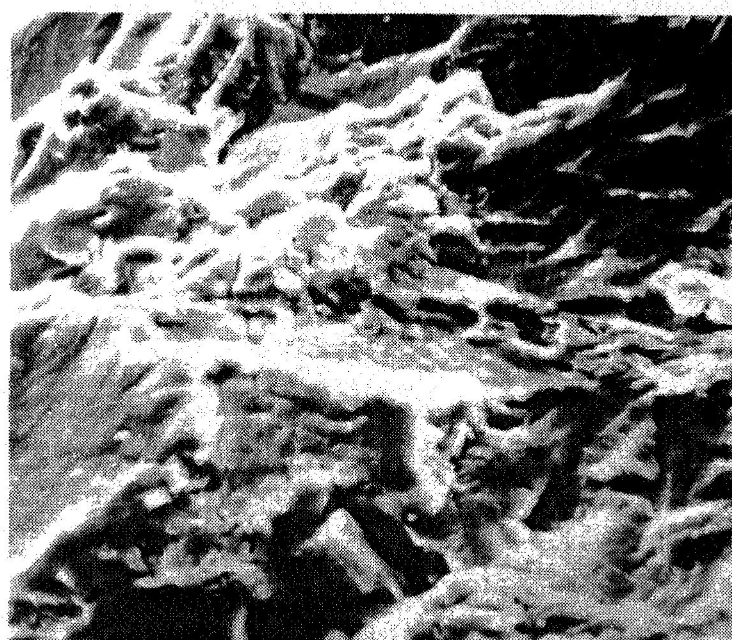
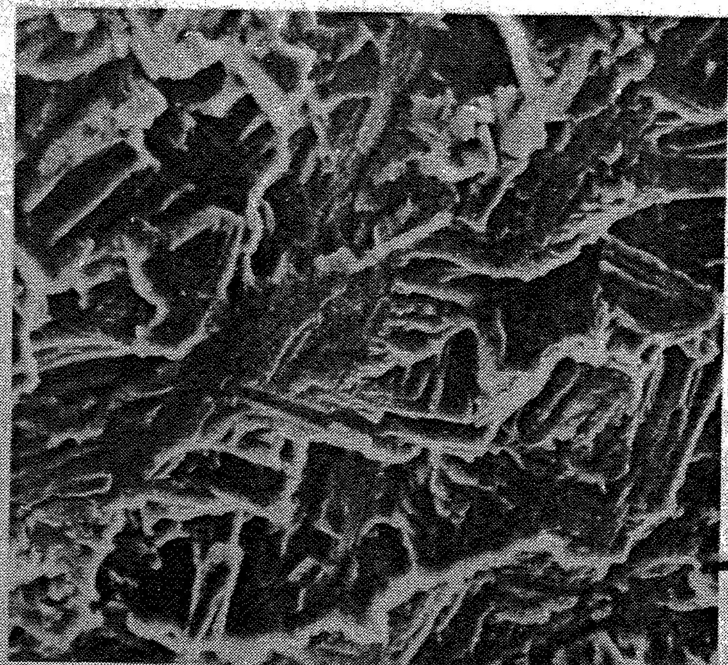


FIGURE 8. FATIGUE BEHAVIOR OF WOL SPECIMENS IN AIR AND 3.5% NaCl

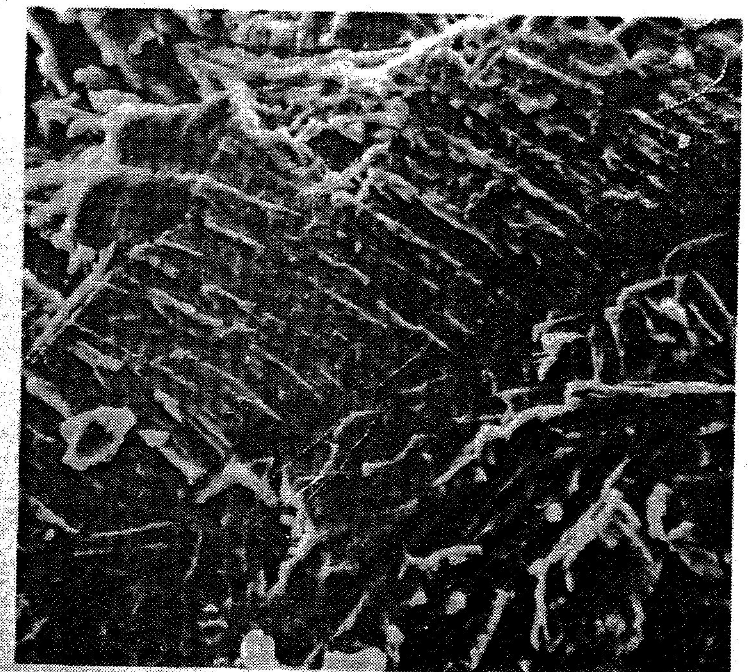


SPECIMEN TWR-6

FIGURE 9. FRACTURE SURFACE OF ANNEALED Ti-6Al-4V FATIGUED IN AIR

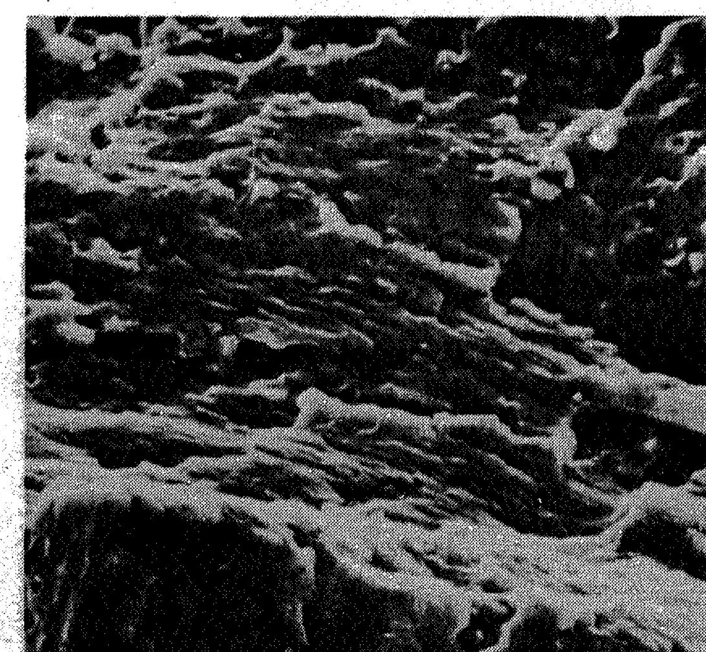

MAG 1000X

QUASI-CLEAVAGE
REGION


MAG 1000X

CLEAVAGE REGION
SPECIMEN TWR-5

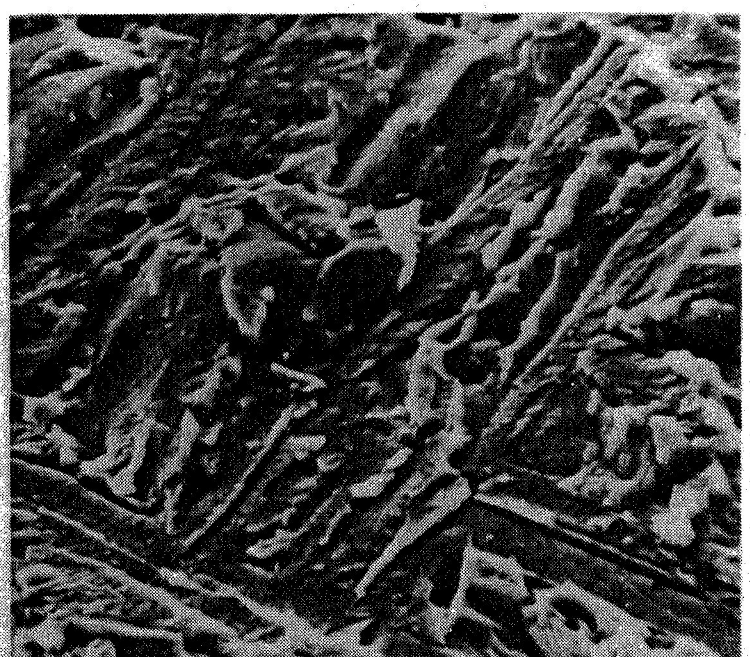
FIGURE 10. FRACTURE SURFACE OF ANNEALED Ti-6Al-4V FATIGUED IN 3.5% Na Cl



MAG
2000X

SPECIMEN W-22

FIGURE 11. FRACTURE SURFACE OF NOR-Ti-BOND JOINT FATIGUED IN AIR



SPECIMEN W-15

FIGURE 12. FRACTURE SURFACE OF NOR-Ti-BOND JOINT FATIGUED IN 3.5% Na Cl

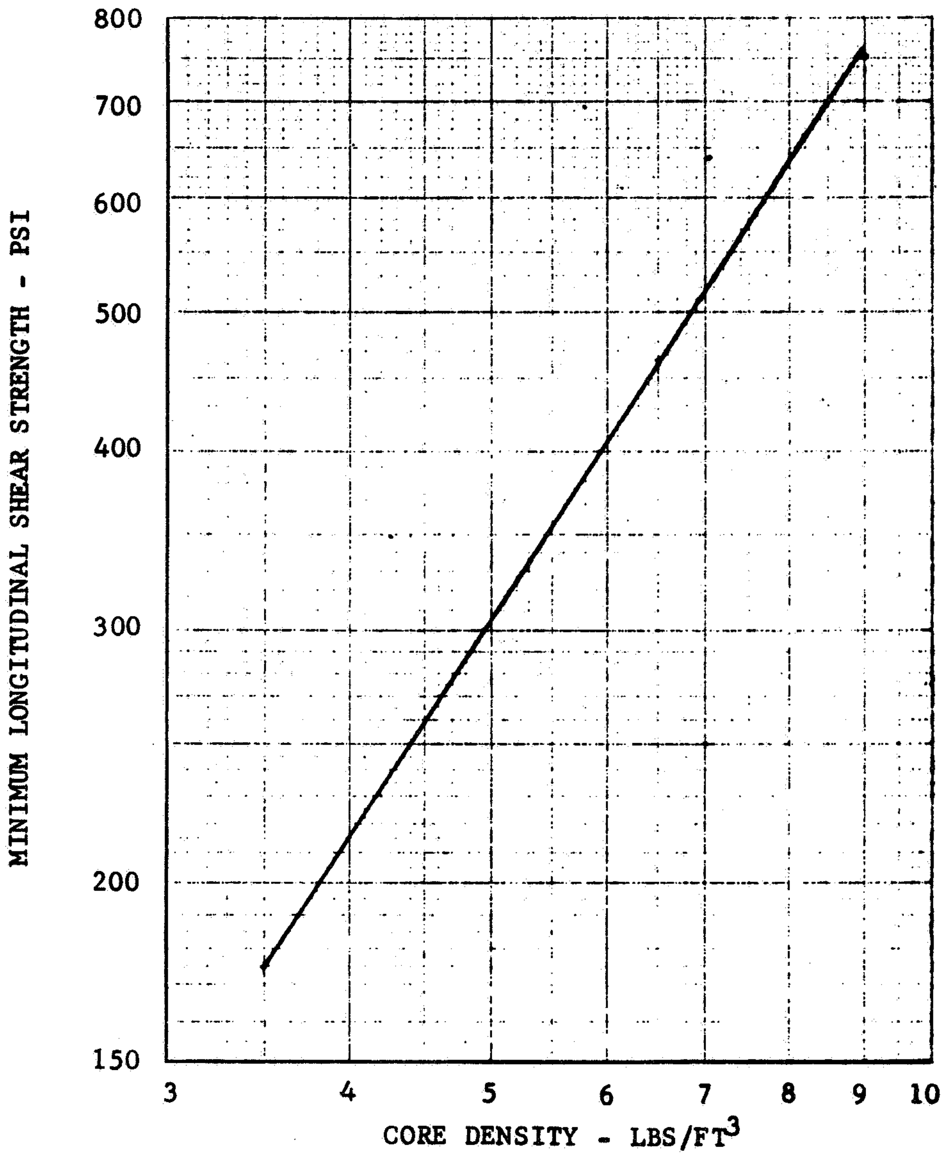


FIGURE 13. ANNEALED Ti-75A TITANIUM NOR-Ti-BOND HONEYCOMB SQUARE CELL — FLAT WALL MINIMUM LONGITUDINAL SHEAR STRENGTH

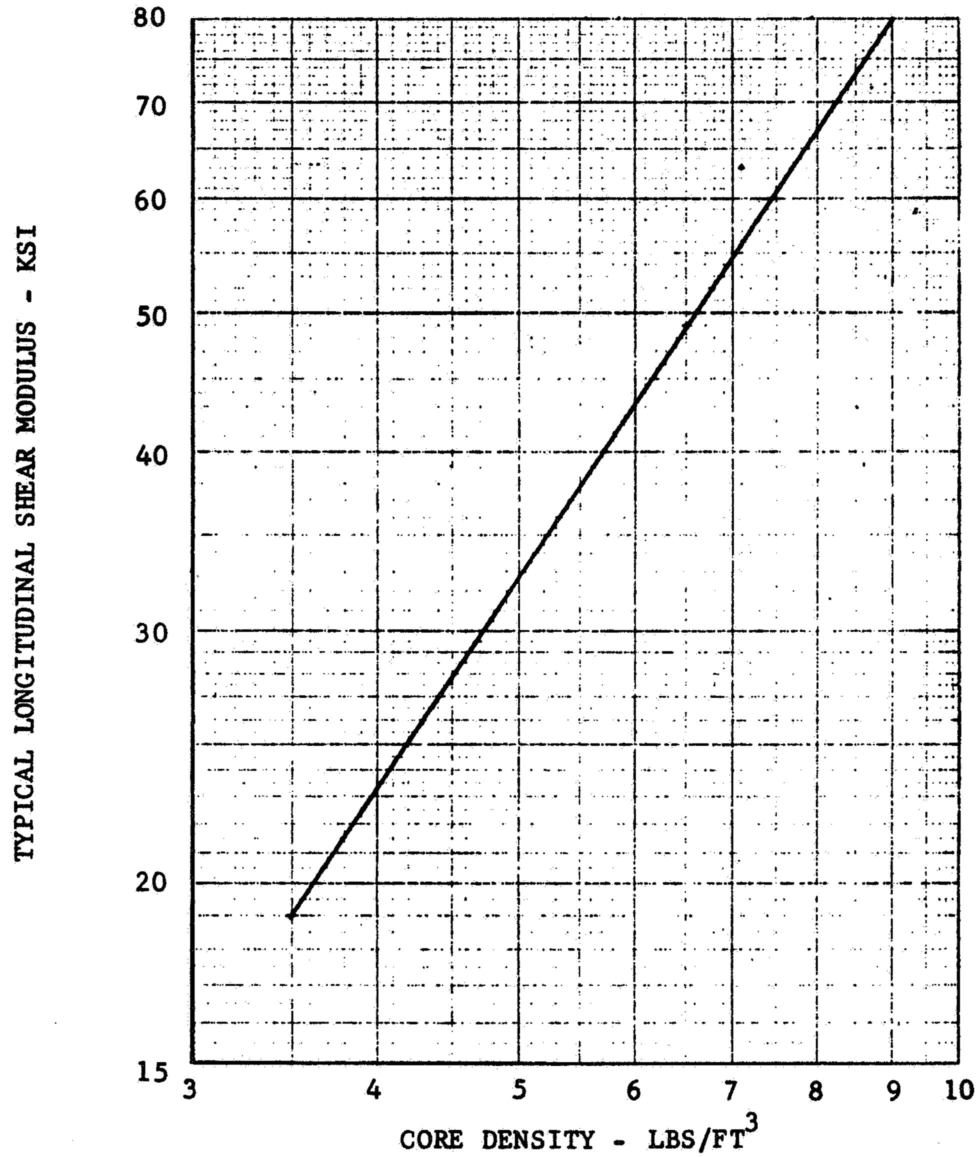


FIGURE 14. ANNEALED Ti-75A TITANIUM NOR-Ti-BOND HONEYCOMB SQUARE CELL — FLAT WALL TYPICAL LONGITUDINAL SHEAR MODULUS

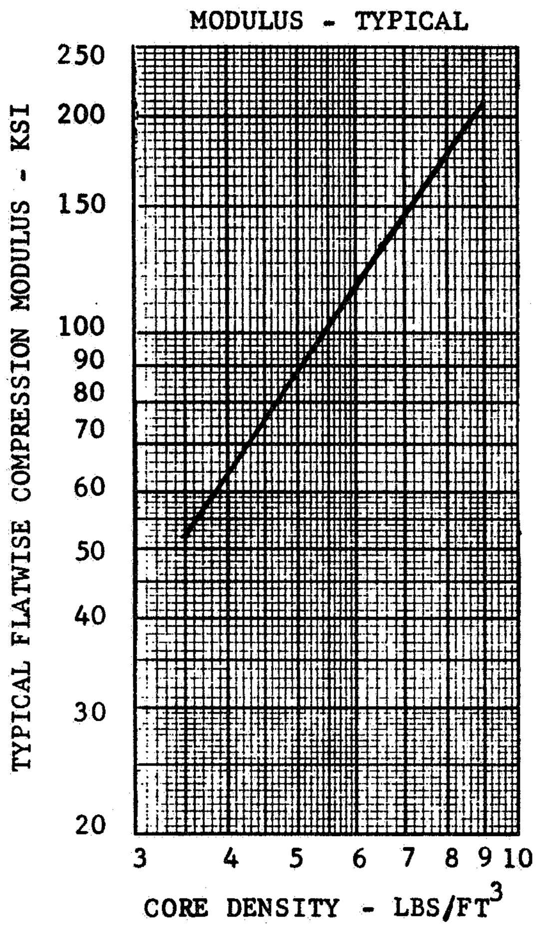


FIGURE 15. ANNEALED Ti-75A TITANIUM NOR-Ti-BOND HONEYCOMB SQUARE CELLS – FLAT WALL FLATWISE COMPRESSION

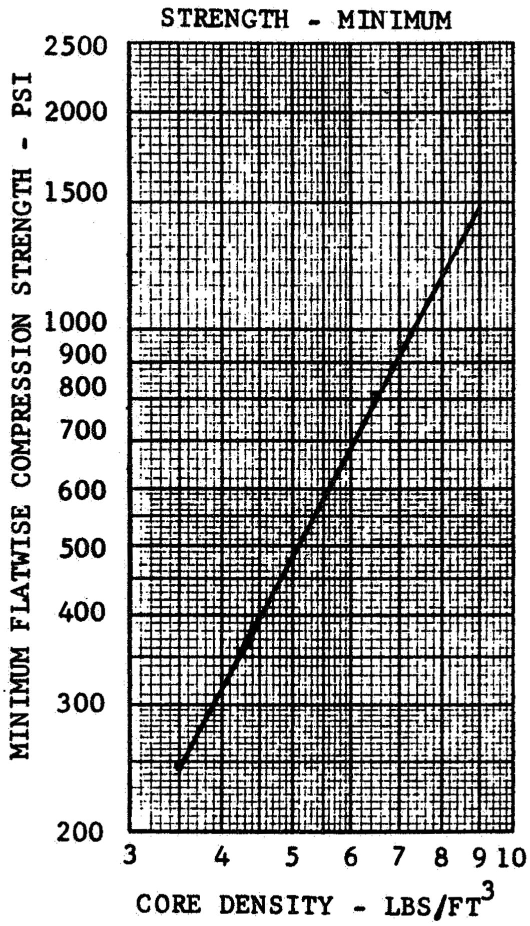


FIGURE 16. ANNEALED Ti-75A TITANIUM NOR-Ti-BOND HONEYCOMB SQUARE CELLS – FLAT WALL FLATWISE COMPRESSION

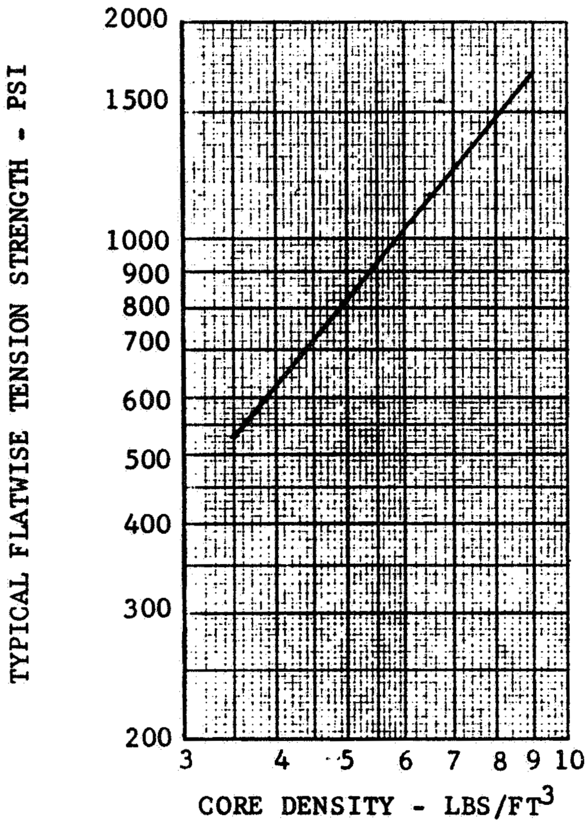


FIGURE 17. ANNEALED Ti-75A TITANIUM NOR-Ti-BOND HONEYCOMB SQUARE CELLS – FLAT WALL TYPICAL FLATWISE TENSION STRENGTH

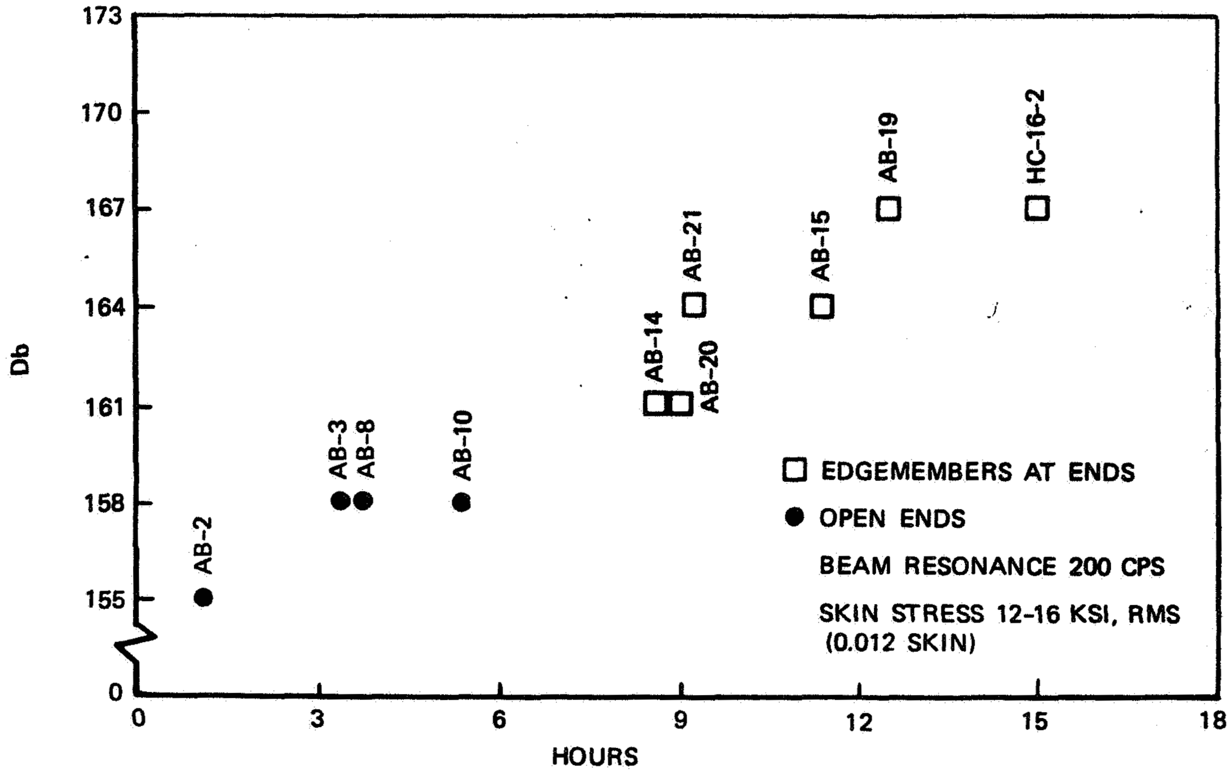


FIGURE 18. ACOUSTIC FATIGUE RESPONSE OF NOR-Ti-BONDED HONEYCOMB BEAMS

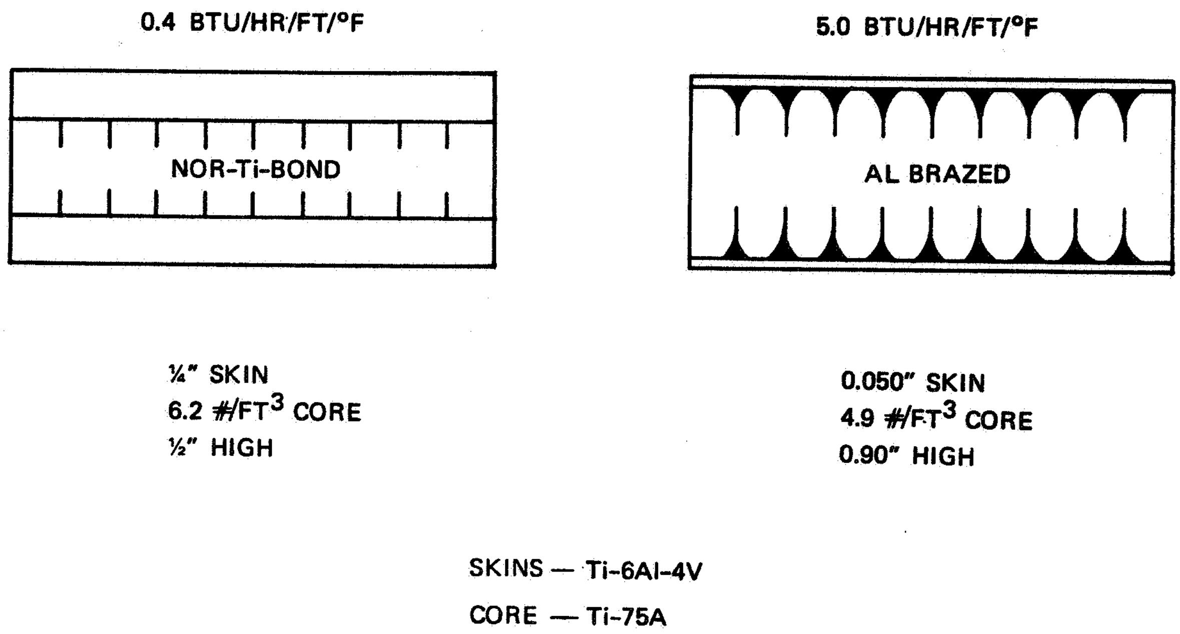


FIGURE 19. THERMAL CONDUCTIVITY OF TITANIUM HONEYCOMB PANELS

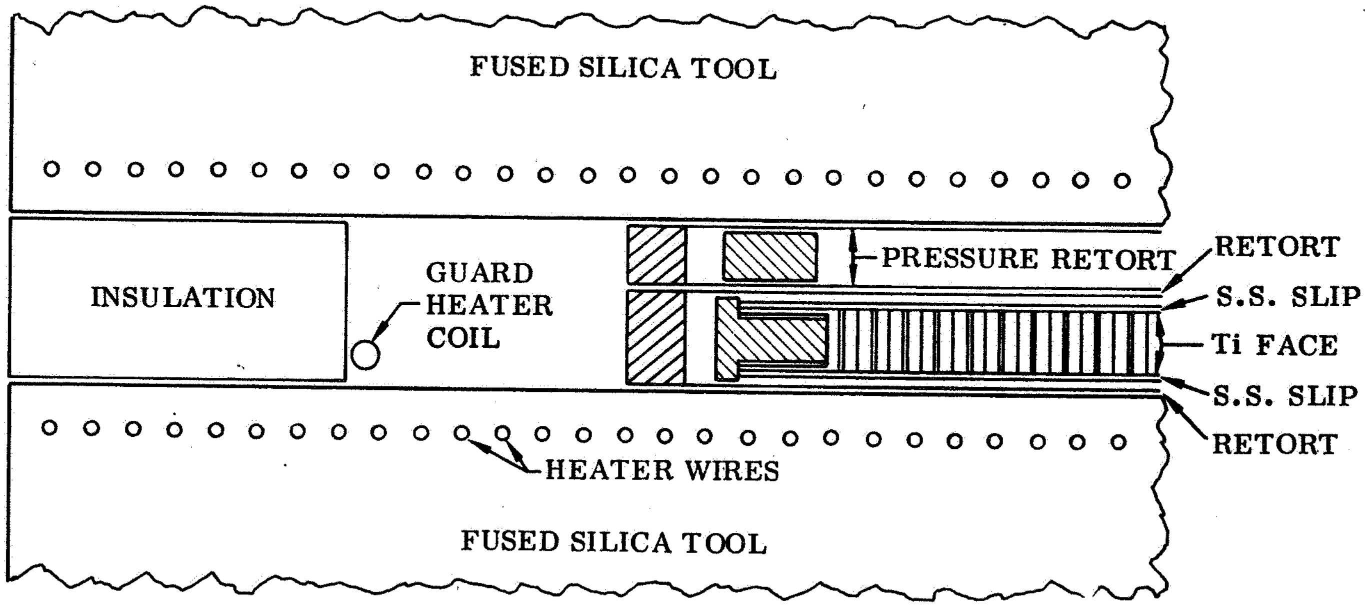


FIGURE 20. BONDING TOOL RELATIONSHIP