Computerized Prediction of Heat Distribution in Weld Tooling—Further Definition

Heat transfer across the base metal-weld tooling interface increases as weld tooling pressure increases, and both cross sectional area of the weld nugget and base metal temperature decrease with increasing tooling pressure

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ABSTRACT. Heat dissipation behavior during welding is a key factor in weld nugget characteristics and properties in the adjacent heat-affected zone and in the base metal. In previous work, a computer node model was developed to correlate mathematically predicted time-temperature profiles in the weld tooling and base metal with those determined experimentally. The correlation was most satisfactory in the base metal, but the weld tooling correlation required improvement. This paper describes the effort to more accurately determine the variables and their influence on the heat transfer across the base metal-weld tooling interface.

Variations of copper chill hardness and tooling pressure during welding experiments indicated that the weld nugget cross-sectional area, base metal temperature, and temperature drop across the aluminum base metal-copper chill interface stabilized at 1,500 psi tooling pressure for full hard copper. This was equivalent to 3% of the compressive yield strength. Annealed copper chills produced lower peak temperatures in the base metal material than did full hard copper at the same tooling pressures. Therefore, lower tooling pressure can be used with annealed copper to stabilize heat transfer across an interface. As a result of these empirical findings, the interface heat-transfer coefficient can be more accurately defined for future computer solutions of welding heat-transfer problems.

Introduction

The tooling used in gas metal arc and gas tungsten arc welding provides two essential functions: the first is mechanical—to maintain joint alignment during welding; the second is metallurgical—to provide chill to the weldment to control bead size and joint properties. The significance of the chilling function, particularly when welding heat-treated alloys, has been realized by McDonnell Douglas Astronautics Company-Western Division (MDAC-WD); therefore, a computer program has been applied to analytically determine heat flow during fusion welding. One of the principal reasons for undertaking this development program was to enable prediction of weld tooling requirements based on heat-transfer requirements for the desired weldment properties, rather than relying on the previous practice of trial and error.

A generalized, three-dimensional heat-transfer computer program was modified for the particular cases of gas metal-arc and gas tungsten-arc fusion welding. This program has been quite successful in describing heat transfer in a weldment. Good overall correlation has been obtained between predicted temperatures in various areas of the weldment and associated tooling, and the temperatures actually measured during weld experiments.[1]

The accuracy of overall temperature predictions using this computer program encouraged a closer analysis of several localized areas where less accurate agreement occurred. One such area of discrepancy (less accurate correlation of titanium weld data than of aluminum data) was resolved by more precise calculation of thermal conductivity (including a factor for the variation of thermal conductivity with temperature), as well as improvements in the computer programming techniques (by using smaller nodes to more accurately describe the molten weld nugget).

A second problem area was that of heat transfer from base metal (i.e., parent material) to weld tooling. The measured temperatures across the tool-to-plate interface were not predicted consistently by the thermodynamic theory used. Therefore, a series of experiments were run to determine which variables are significant in affecting heat transfer from a weldment into the associated weld tooling. The results of these experiments were then used to incorporate the significant heat-transfer variables into the thermodynamic theory used, and to improve the correlation between prediction and experiment. The results of these experimental evaluations of heat transfer from a weldment into weld tooling are described in this paper.

Procedure

Material

Aluminum alloy 2014-T6 was selected as the material for welding. Panel size was ⅜ x 12 x 18 in. The material procured for chill bars was an electrolytic tough-pitch copper in the full hard condition, with a compressive yield strength of 51,020 psi. This same material was annealed to a compressive yield strength of 9,100 psi for those tests requiring the use of fully annealed copper chill bars. After welding, the compressive yield strength of the annealed material was found to have increased to 12,800 psi.

Two types of interleaf materials were used between the copper chills and the aluminum plate. Radiographic quality, 0.005 in. thick lead foil was used for one test weld, and a double layer of fiberglass cloth, type 1533,

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was used in two other test welds.

Weld Fixture

A weld fixture was designed to apply preselected pressures to the chill bars—Fig. 1. A compressed air system was employed, capable of providing 5,000 psi on the ¼ in. wide chill bar surfaces, by use of three air hoses. Hose pressure was related to actual chill pressure by placing the weld fixture in a tensile testing machine and determining the loads experienced as the hose pressure was varied. The hold-down clamps were fabricated from $1^3/_4$ in. stainless steel plate to permit no more than 0.01 in. deflection with application of maximum pressure.

It was necessary to prevent introduction of any externally induced bending moment into the weld during solidification. For this reason, the top and bottom chill bars were placed in direct vertical alignment. A decrease in contact area between plate and chill bars would have been caused by bending. It was also possible to limit heat conduction to just the chills by supporting the specimens in this fashion, and eliminating contact with any other portion of the tool.

Aluminum was selected for all remaining components of the weld fixture. This was done so that the incidence of magnetic arc wander would not disturb exact centerline tracking of the weld, and that relationship of the weld to the pre-set location of the thermocouples would remain constant.

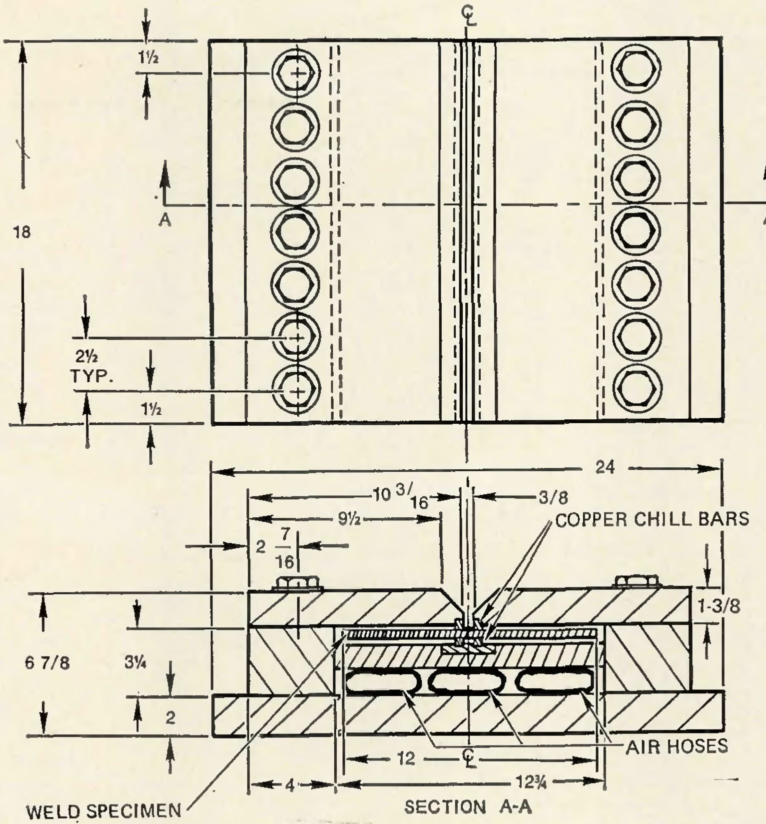

Fig. 1—Weld fixture

Welding Equipment

The welding power supply, shown in Fig. 2, is a 600 amp, d-c, magnetic-amplifier type with provisions for gas metal-arc or gas tungsten-arc control. Inasmuch as this equipment differed from that used previously,[1] a temperature verification run was performed on an instrumented test panel with the original weld fixture and clamping pressure. The resultant temperature traces showed excellent agreement, the only differences being attributable to ambient temperature variations during the respective tests.

The weld fixture was located on a steel table directly under a side-beam carriage. Welding parameters were developed that produced slightly over 50% penetration with a bead-on-plate technique, without any tooling chill. This level of penetration was shown in preliminary test welds to be the most effective in demonstrating the change in weld nugget cross-sectional area as tooling chill was applied. These welding parameters (i.e., controlled welding conditions for experiment), which were held constant for all subsequent test welds, were as follows: arc voltage—12 v, welding current—225 amp; torch travel speed—9 ipm; shielding gas—helium at 80 cfh; shielding gas orifice diameter—⅝ in.; electrode—$^3/_{32}$ in. diameter, 2% thoriated tungsten; electrode geometry—12 deg included angle truncated cone with 0.050 in. flat.

Temperature Measurement

Nine chromel-alumel thermocouples were placed selectively within the specimen and weld fixture as shown in Fig. 3. These thermocouples were used so that the temperature data generated could be compared at a later date with computer-predicted temperature profiles. Of interest in this paper are the temperatures measured in the base metal (TC 3) and those on each side of the top and bottom interfaces (TC 4, 5, 7, and 8).

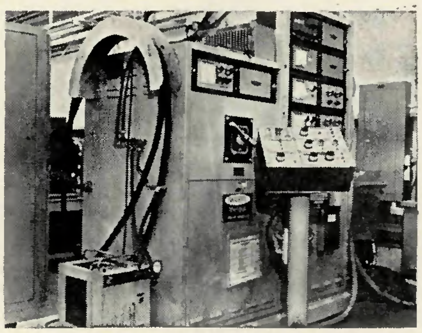
Fig. 2—Power source

Thermocouple installation was greatly facilitated by the use of ceramic-encased chromel-alumel wires. The thermocouples were swaged into location after installation in a drilled pilot hole.

A MDAC-WD-built recording system, shown in Fig. 4, was employed for temperature recording. The outputs from the nine thermocouples were fed into a precalibrated channel which, in turn, was amplified for transmission and recording on magnetic tape. Concurrently, elapsed weld time and event marker, or pulse, were recorded. These data were submitted subsequently to a digital computer programmed for their reception. The sampling rate for each thermocouple was 25 times/sec, which was more than adequate to capture any peak temperature. A visual representation of the magnitude of temperature in each channel was portrayed on the oscillograph monitor. It was also possible to see the heat wave move longitudinally in the weld specimen by the change in amplitude of the respective channels on the monitor.

Temperature data for all thermocouples for the ten runs were plotted separately on a Calcomp printer.

Results and Discussion

Effect of Pressure on Heat Transfer

The effect of chill bar pressure variation on heat transfer was first exam-

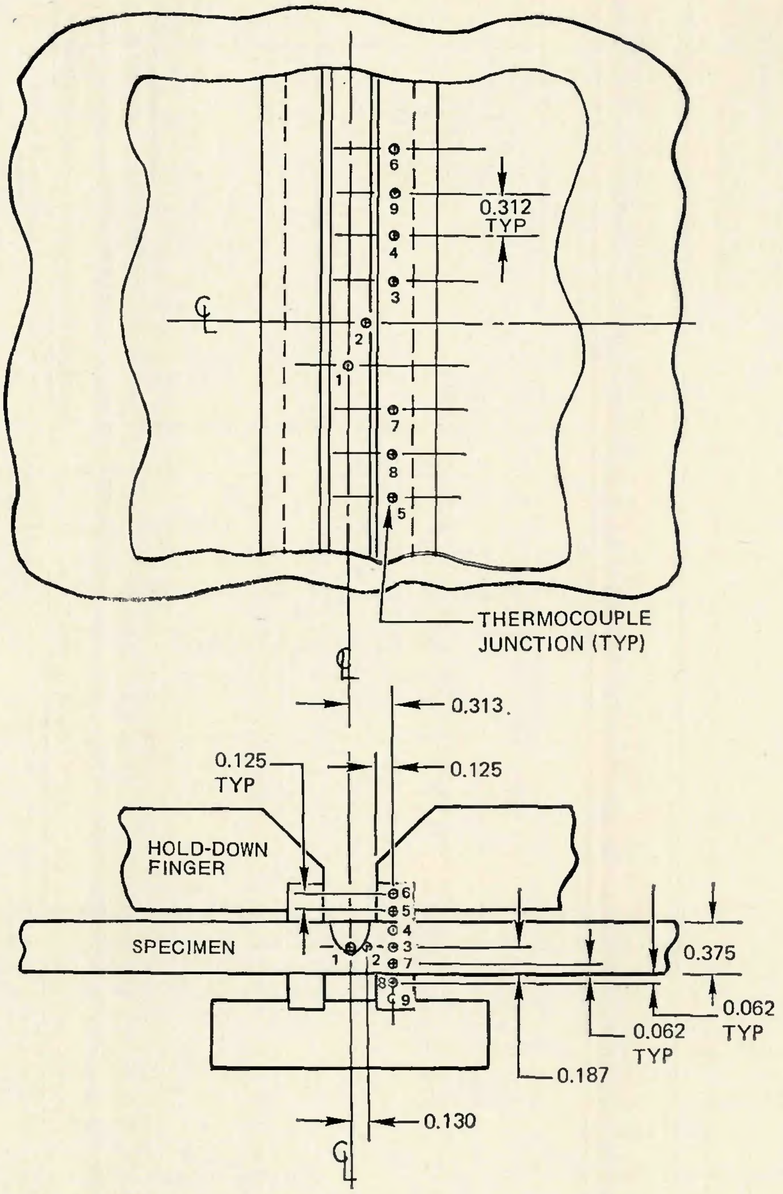

Fig. 3—Thermocouple locations

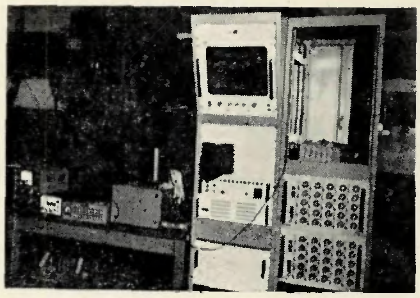

Fig. 4—Recording system

ined. Using full hard copper chill bars on the top and bottom sides of the specimen, four pressure levels between 390 and 3,145 psi were employed for test runs 1 through 4, as shown in Table 1.

As the pressure was increased on the chill bars in the hose system, it was found that the peak temperatures attained in the base metal at the bottom chill interface (TC 7)—decreased exponentially from 700° F to a value of 460° F at a chill pressure of 777 psi—Fig. 5. Similarly, the temperature in the lower full hard copper chill increased from 80° F to 370° F, at the same pressure, with a resultant Δt of 90° F across the tool-base metal interface.

This differential remained fairly constant to a 3,145 psi chill pressure, indicating a level of stability and effectiveness in transferring heat. Thus, by quadrupling the pressure above 777 psi, (3% of the compressive yield strength of the full hard copper) the Δt across the lower interface decreased by only 10° F. The significance of this finding is that it is not necessary to use high tooling pressure to obtain efficient cooling, nor is it prudent to construct the massive tools necessary to handle high pressures, when the improvement in heat transfer is so slight.

Effect of Pressure on Weld Bead Size

The cross-sectional area of the weld bead, resulting from varying chill bar pressure, was measured and is shown in Fig. 6. These areas were measured with a planimeter, and are plotted against the lower interfacial temperature differential Δt in Fig. 7. Since the welding parameters were held constant, the observed differences are believed to be due solely to chilling efficiency (i.e., chill pressure).

Excellent correlation was obtained between Δt and bead size, because, as heat-transfer efficiency increases (increase in chill pressure and decrease in Δt), the weld bead size decreases (up to a pressure of 800 psi and a Δt of 90° F). Beyond this point, while Δt continues to decrease slightly, there is no further appreciable change in weld bead size. Again, high tooling pressures do not offer improvements in weld bead size com-

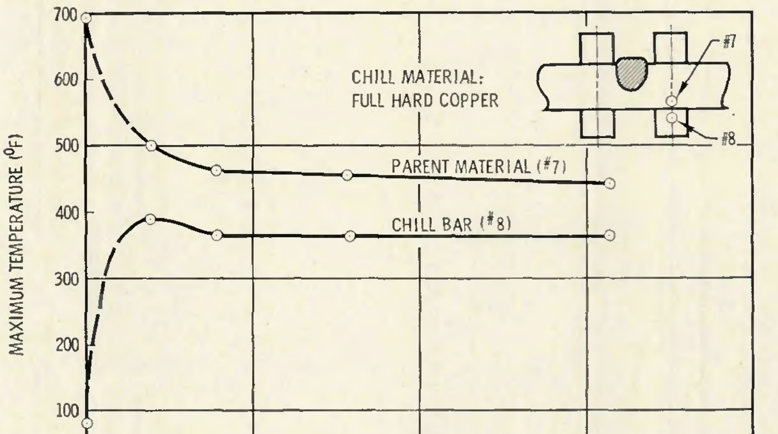

Fig. 5—Effect of tooling pressure on heat transfer across lower interface

mensurate with the additional expense involved.

Effect of Upper Chill Interface on Heat Transfer

The peak temperatures attained across the upper interface of the 2014 plate (TC 4), and the upper full hard copper chill bar (TC 5), are shown in Fig. 8. The same trends are visible here as were observed in the lower chill; i.e., an exponential decrease in aluminum plate temperature with an increase in pressure and a corresponding increase in tooling temperature. The temperature difference at the interface (Δt) again decreases rapidly, this time up to the 400 psi pressure condition, followed by a slow decrease at pressures above this point.

There are two significant differences between these results, as compared to those observed at the lower interface. First, the absolute temperature values of the tooling are higher; second, the value of Δt not only reaches a minimum at a lower chill pressure but also has a lower value (20° F vs. 90° F) for the lower chill. The higher peak temperature is probably due to radiant heating from the arc.

Influence of the Heat Treat Condition of Chill Material

Two welds were performed using solution-annealed copper chills, placed in contact with the base metal, below the specimen. The chill pressure applied was 1,590 psi for the first weld (reference run 9), followed by 3,145 psi for the subsequent weld (reference run 10), without any interim anneal. The peak temperatures attained at a thermocouple site located at a depth of one-half the material thickness (reference TC 3) are shown in Fig. 9 compared to peak temperatures reached with the use of the full hard copper chill bars. The 10° F lower peak temperature reached with the use of annealed copper was consistent for both applied pressures.

This difference is believed caused by the higher thermal conductivity of annealed copper compared with full hard copper (3.766 watt $cm^{-1}K^{-1}$ compared to 3.682 watt $cm^{-1}K^{-1}$ at 300° F).[2] It is also believed that surface asperities of lower yield point materials are more easily deformed, thus increasing heat transfer by creating additional interfacial contact area.

Theoretical Aspects of Heat Transfer Across Interfaces

Heat transfer across an interface is accounted for in terms of a heat transfer coefficient, h. It is found in the expression

$$Q_{m-n} = h_{m-n}A(T_m - T_n)$$

where Q_{m-n} denotes heat transfer between surface m and n in BTU/hr; h_{m-n} denotes heat transfer coefficient between surfaces m and n in BTU/hr-ft² – °F; A denotes area of interface; T_m and T_n denotes temperatures on each side of interface.

To assume h as a constant across

Table 1—Conditions of Weld Tests

Test run	Hose, pressure, psig	Calculated chill pressure, psi	Stress, % CYS of chill material	Insulation or shim Top	Insulation or shim Bottom	Copper condition
1	20	390	0.75	None	None	Full hard
2	40	777	1.5	None	None	Full hard
3	80	1,590	3.1	None	None	Full hard
4	160	3,145	6.1	None	None	Full hard
5	30	590	1.1	2 layers fiberglass	2 layers fiberglass	Full hard
6	160	3,145	6.1	None	None	Full hard
7	80	1,590	3.1	None	0.005T Pb	Full hard
8	80	1,590	3.1	None	2 layers fiberglass	Full hard
9	80	1,590	17.5	None	None	Annealed
10	160	3,145	24.5	None	None	Annealed

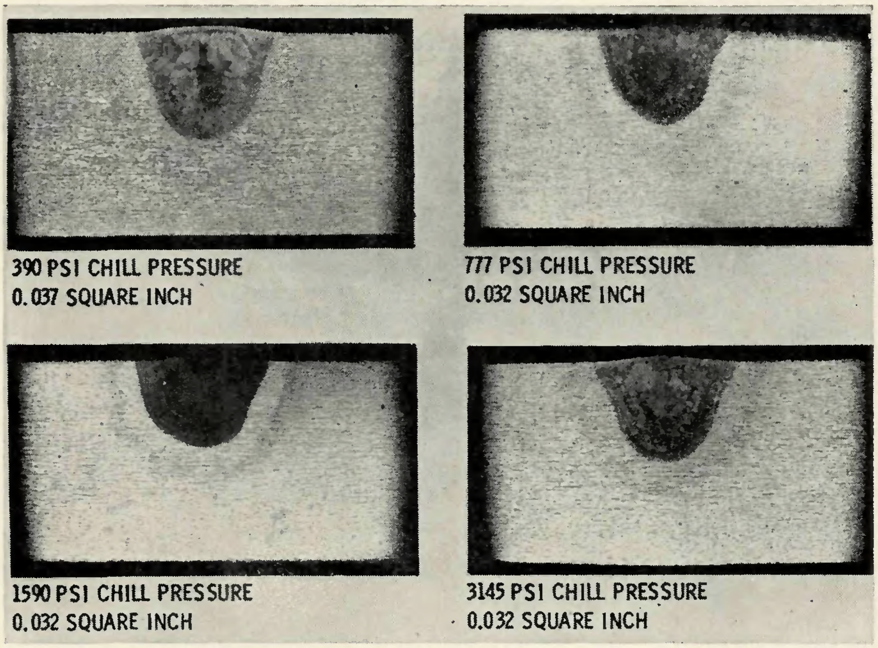

Fig. 6—Effect of chill pressure on nugget area

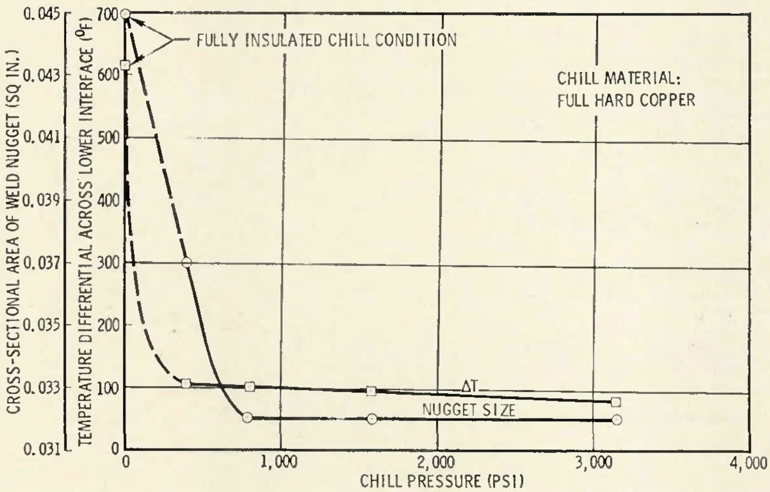

Fig. 7—Effect of pressure on weld bead size

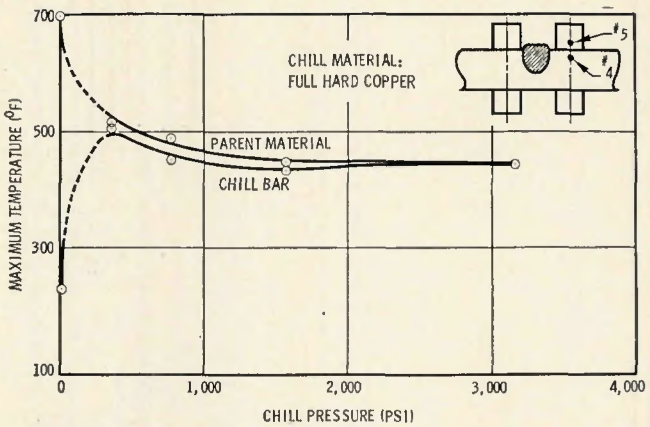

Fig. 8—Effect of tooling pressure on heat transfer across upper interface

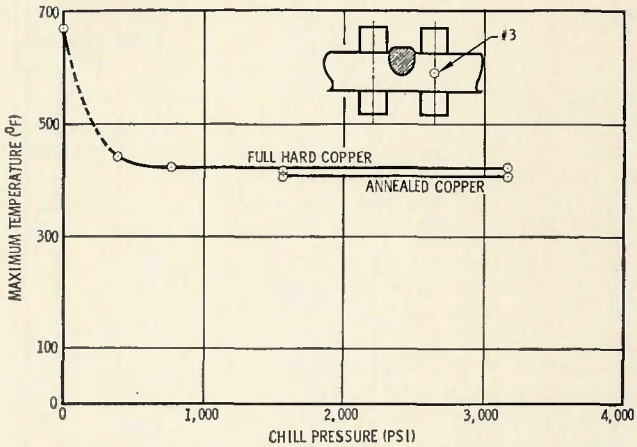

Fig. 9—Influence of chill material on base metal temperature

adjacent interfaces is easier for computational reasons. However, through considerations involving the kinetic theory of gases, it can be shown that h is inversely proportional to the square root of the absolute temperature of the gas medium trapped between the faying surfaces in question. Since a temperature rise is definitely experienced in welding, then h as a function of temperature would yield more accurate results by use of a computer to analytically predict interfacial heat transfer. This dependence of h on temperature is further complemented by the work of Dutkiewicz,[3] who showed that the dominant parameters in heat transfer across randomly rough surfaces are:

1. Mating pressure.
2. Surface roughness.
3. Compressive yield strength of the softer of the two interfaces in contact.
4. The thermodynamic properties of the gas trapped between the faying surfaces.

The previous discussion on the effect of tooling pressure stabilizing the temperature differential across the chill-base metal interface substantiates the first of these findings.

As regards the compressive yield strength, it is shown in Fig. 10 that the peak temperature remains fairly constant after 3% of the compressive yield strength of the chill material is reached. These data include both those for the full hard and annealed copper test runs. Even though an increase in the compressive yield strength of the annealed copper was observed after it was subjected to tooling pressure and angular distortion of the base metal specimen during welding, the peak temperature in the lower chill bar remained constant. It can be postulated that the effects of hardening experienced in the annealed chill bar are insignificant to interfacial heat transfer because the chill pressure represents 25% of the compressive yield strength—well above the 3% apparent minimum for temperature stabilization. The significance of using annealed copper as a chill material is that far lower pressures would be required in the tooling system. As an example; 3% of 50,000 psi (compressive yield strength of full hard copper) is 1,500 psi; whereas, 3% of 10,000 psi (compressive yield strength of annealed copper) is 300 psi—a ratio of 5:1.

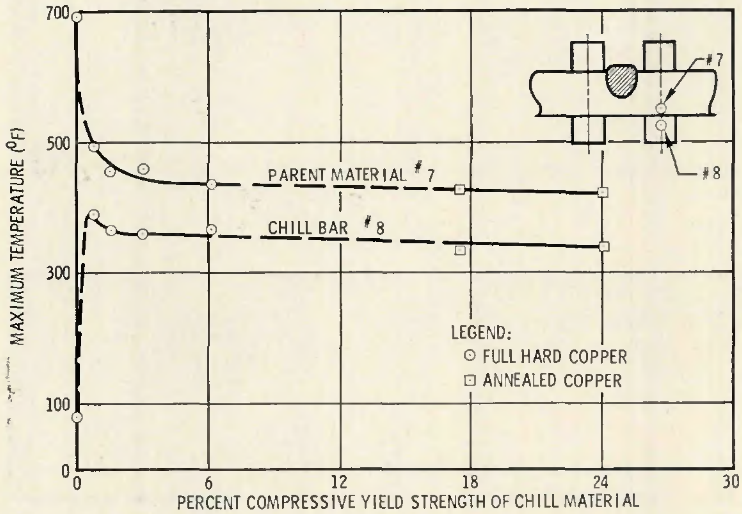

Fig. 10—Effect of compressive yield strength of chill material on heat transfer

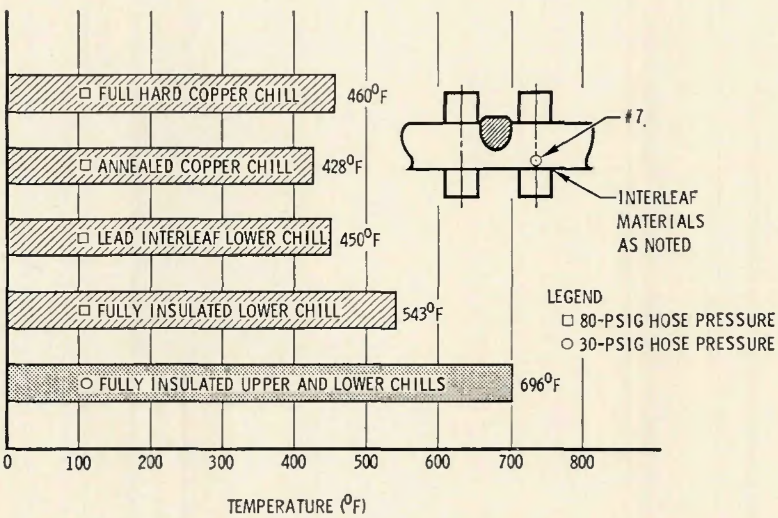

Fig. 11—Effect of chill condition on base metal peak temperature

Effect of Interleaf Material

The above findings once again substantiate the work of Dutkiewicz, and suggest that a chill material having a lower compressive yield strength than annealed copper would permit the use of even lower tooling pressures. Thus, a test weld was performed using 0.005 in. thick lead as an interfacial shim between the base metal and full hard copper (reference run 7). The peak temperature attained in the base metal (TC site 7) is shown and compared with the other chill conditions at identical pressures in Fig. 11 (except for 590 psi pressure on a fiberglass tape interleaf).

The lead shim was more effective than the full hard copper, lowering the temperature in the specimen 10° F. The specimen temperature was 22.5° F higher when the load condition was compared to that of annealed copper. This finding fails to substantiate the compressive yield strength postulation, possibly because an additional interface exists with the lead shim. Even though the lead shim may have served to increase the interfacial contact area, as observed in the 10° F improvement over full hard copper, the presence of two interfaces must contain more entrapped gas than just one.

Fiberglass tape on test welds 5 and 8 was not employed to enhance thermal conductivity across the interface, but was used as a basis of comparison. As would be expected, the peak temperature in the base metal was 83° F higher with fiberglass tape at the lower interface only, and 236° F higher with the fiberglass tape at both interfaces, when compared to the full hard copper test condition.

The results of these tests portray the no-chill condition frequently encountered in the soft tooling approach used in aerospace welding and fabrication sequences. They demonstrate the contribution of the base metal in chilling the weldment, without the aid of supplemental chill to minimize the heat-affected zone; in increasing cooling rates, and in controlling weld bead size.

Conclusions

The following conclusions have been derived from the work reported in this paper:

1. Heat transfer across the base metal-weld tooling interface increases with an increase in weld tooling pressure.

2. The cross-sectional area of the weld nugget and the temperature of the weld specimen decrease with increasing tooling pressure.

3. A critical tooling pressure was found, above which a substantial decrease in weld bead size and interface temperature were not observed. Therefore, it is uneconomical to design and fabricate weld tooling that exceeds this value.

4. Annealed copper chills produced lower peak temperature in base metal than did full hard copper at the same tool pressures. The improvement in heat transfer into the tool is believed to be caused by the higher thermal conductivity of annealed copper and the increased interface contact area resulting from the lower yield strength. This finding suggests that lower tooling pressures are permissible with the use of annealed copper to stabilize heat transfer across an interface.

5. The relationship of base metal pressure and compressive yield strength to heat transfer across a base metal—weld tooling interface demonstrate the applicability of the findings of Dutkiewicz on heat transfer across metallic interfaces.

6. Ductile lead interleaf material with cold-worked chill bars was not as effective in reducing peak temperatures in the weld specimen as was the annealed copper chill.

7. As a result of these empirical findings, the heat transfer coefficient, h_{m-n}, across the base metal-chill interface, can be more accurately defined for future computer solutions of welding heat transfer problems.

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