

from the weld interface, and q_0 is the magnitude of the surface heat flux.

The next significant contribution was made by Cheng (Refs. 6, 7), who analyzed both similar and dissimilar tubular joint configurations. Cheng numerically solved the differential equation of heat conduction (see Equation 2, which follows) with appropriate boundary conditions, and also allowed the material properties to vary with temperature (Ref. 6):

$$\frac{T}{t} + U(t) \frac{T}{x} = \frac{1}{C_p} \frac{1}{x} k \frac{T}{x} - \frac{P}{C_p A} (T^4 - T_o^4) - \frac{hP}{C_p A} (T - T_o) \quad (2)$$

where A is the cross-sectional area, T_o is the ambient temperature surrounding the tube, P is the outer perimeter of the tube, C_p is the specific heat, ρ is the density, h is the film coefficient of heat transfer (convective cooling), $U(t)$ is the velocity of the melt front, ϵ is the emissivity, and σ is the Stefan-Boltzmann constant. Cheng allowed for the existence of a melt layer by incorporating a moving boundary term. Several experimental studies have refuted the notion of melting during friction welding, such as the work of Weiss and Hazlett (Ref. 8), and this topic will be revisited later in the present work. As Wang (Ref. 9) pointed out, it is quite likely that softened material at temperatures near the melting point will be expelled as flash before melting can occur.

Wang and Nagappan (Ref. 10) performed a thermal analysis similar to that of Cheng but for the inertia welding of steel bars. Their predictions showed the peak temperature to be less than the melting point and that for inertia welding, the peak temperatures are achieved very quickly as compared to conventional friction welding. Additionally, they noted a strong dependence of the pre-

dicted temperature distribution on the total welding time. This total welding time is a function of all the main process variables: initial rotational speed, thrust pressure, moment of inertia, etc. Their model has good qualitative agreement with measured temperature values.

Johnson, *et al.* (Ref. 11), have also noted the power dissipation curve for inertia welding is very different than that for friction welding. They have suggested a two-part curve: Stage I, corresponding to a more concentrated initial contact and Stage II, a slower (relatively slower) decay. They proposed the following functional forms:

$$\begin{aligned} \text{Stage I: } q(t) &= q_{\max} \sin(\omega t) \\ \text{Stage II: } q(t) &= T_{\max} \sqrt{\frac{k\rho C_p}{\pi t}} \end{aligned} \quad (3)$$

where q_{\max} is the maximum power dissipation and T_{\max} is the maximum interface temperature attained — Fig. 1. A more recent multistage thermal model for direct-drive friction welding was developed by Midling and Grong (Ref. 12), who proposed various analytical forms for the heating stage, steady-state condition, and cooling stage based on continuous planar disc sources at the weld interface.

There are numerous finite element and finite difference models on both conventional friction and inertia welding. These modeling efforts account for the heat generation term by examining the coupled thermomechanical problem together with an interfacial friction law. This interfacial friction law or constitutive relation must account for frictional heating. Sluzalec (Refs. 13, 14) was one of the first to use the finite element analysis (FEA) approach for friction welding, and Moal, *et al.* (Refs. 15, 16), have developed an FEA model specifically for inertia welding. Sahin, *et al.* (Refs. 17, 18),

have produced a series of finite difference models. Weiss (Ref. 19) has also investigated the residual stresses after welding using the FEA approach. Fu and Duan (Ref. 20) have more recently used the FEA approach to model the axial pressure distribution in addition to the temperature field.

As mentioned earlier, this work is a reduced order model of the inertia welding process. It is motivated by the need to have simple, yet realistic, models that can be used for *in-situ* process monitoring and control and for rapid parameter development and validation. It differs from previous works in that it attempts to more accurately capture heat generation during welding as a function of time by using data routinely gathered by the inertia welding machine. This data is directly used as an input to the thermal simulation, which in this case is a reduced-order analytical model of heat conduction. As such, this approach could also be used with more sophisticated models for heat transfer, and would provide a reasonable estimate of heat generation without having to explicitly model the combined thermomechanical problem. Also, this approach is amenable to an on-line monitoring strategy that flags potentially defective welds in critical components and has the potential to alleviate the inspection burden by reducing it to “inspection for cause” as opposed to inspecting every component.

Equipment and Experimental Procedure

The commercially pure niobium and 316L stainless steel utilized in this study were in the form of 1 in.-diameter tubes. The Nb tube wall thickness was slightly thicker (0.125-in.) than the 316L tube wall (0.08 in.) to provide for greater forging action during the upset stage. Prior to

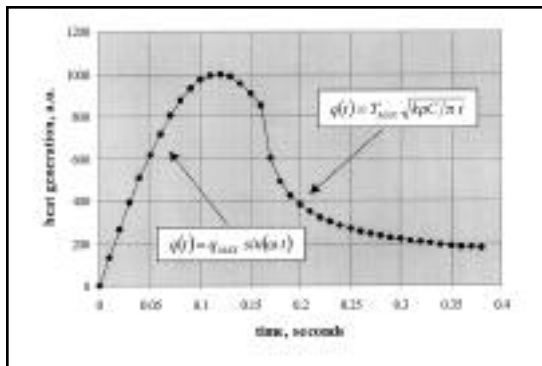


Fig. 1 — Schematic heat generation term after Johnson, *et al.* (Ref. 11).

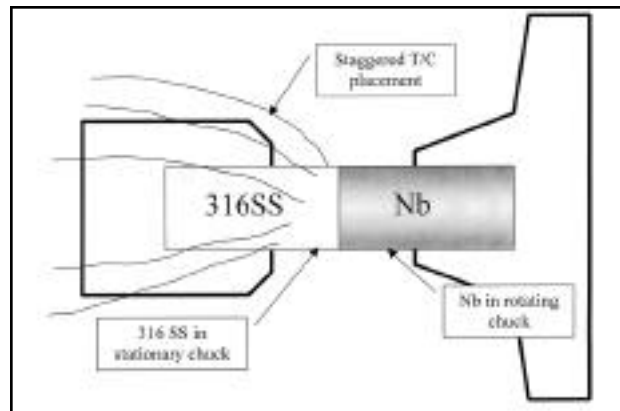


Fig. 2 — Schematic showing thermocouple placement.

