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HOT-WIRE ANEMOMETRY IN TRANSONIC FLOW

C. C. Horstman
and
W. C. Rose

Ames Research Center
Moffett Field, Calif. 94035

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16. Abstract

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C. C. Horstman and W. C. Rose

SUMMARY

The use of hot-wire anemometry for obtaining fluctuating data in transonic flows has been evaluated. From hot-wire heat loss correlations based on previous transonic data, the sensitivity coefficients for velocity, density, and total temperature fluctuations have been calculated for a wide range of test conditions and sensor parameters. For sensor Reynolds numbers greater than 20 and high sensor overheat ratios, the velocity sensitivity remains independent of Mach number and equal to the density sensitivity. These conclusions were verified by comparisons of predicted sensitivities with those from recent direct calibrations in transonic flows. Based on these results, techniques are presented to obtain meaningful measurements of fluctuating velocity, density, and Reynolds shear stress using hot-wire and hot-film anemometers. Examples of these measurements are presented for two transonic boundary layers.

Nomenclature

A'_w	overheat parameters, $1/2\left(\frac{\partial \ln R_w}{\partial \ln I}\right)$
c_p	specific heat at constant pressure
d	wire diameter
E	wire voltage
I	wire current
k	heat conductivity
K	$d \ln R_w / d \ln T_w$
ℓ	wire length
m	$d \ln \mu / d \ln T_w$
M	Mach number
n	$d \ln k / d \ln T_w$
Nu	Nusselt number
Pr_t	turbulent Prandtl number
R	resistance
Re	Reynolds number, $\rho u d/\mu$

$R_{(\rho u)T_t}$	correlation coefficient of mass-flux and total temperature fluctuations, $\frac{[\overline{(\rho u)' T_t'}]}{[<(\rho u)'> < T_t'>]}$
S	sensor sensitivity coefficient
T	temperature
u, v, w	axial, normal, and spanwise velocity
y	distance normal to wall
α	$\frac{1}{[1 + \frac{(\gamma - 1)}{2} M^2]}$
β	$\frac{(\gamma - 1)M}{[1 + \frac{(\gamma - 1)}{2} M^2]}$
δ	boundary-layer thickness
η	recovery factor, T_r/T_t
μ	viscosity
ρ	density
τ_{wr}	temperature overheat, $\frac{(T_w - T_r)}{T_r}$
<( )>	root mean square

<u>Superscripts</u>

( )'	fluctuating valve
$\overline{( )}$	time averaged

<u>Subscripts</u>

e	boundary-layer edge
r	recovery or adiabatic wall
t	total or stagnation conditions
T	temperature
u	velocity
w	wire
ρ	density
ρu	mass flux
ϕ	flow angle

INTRODUCTION

With the rapid advances in computational fluid dynamics over the past few years, computations that were not feasible several years ago are now being performed routinely. These advances, however, have not been followed by advances in the knowledge concerning the physics of complicated fluid flows (ref. 1). One area of current interest is the numerical simulation of transonic flow about aerodynamic bodies. Recent transonic flow calculations (ref. 2) have shown that the turbulence model employed strongly affects the calculated flow field and that none of the existing models adequately predicts the experimental results. To date, the development of new turbulence models has relied on intuition and measured mean flow data. The direct measurement of the required turbulence quantities would provide data that could be used for the development of improved turbulence models and provide additional insight into the physics of turbulence. Unfortunately, fluctuating turbulence data are virtually nonexistent for transonic flows.

In principle, the hot-wire anemometer can be used to obtain fluctuating velocity, density, and shear stress measurements in transonic flows. However, hot-wire measurements have not been exploited in transonic flows for several reasons. These include the difficulties in determining accurate sensitivity coefficients as well as wire breakage, vibration, and strain gauging problems associated with the high dynamic pressures incurred at transonic flows. The principle problem in determining accurate sensitivity coefficients is that the velocity and density sensitivity coefficients vary with Mach number and, in general, are not equal (refs. 3,4). Previous calculations (ref. 3), based on calibration data available at the time, indicated that the velocity sensitivity coefficients varied by an order of magnitude with small changes in Mach number. This makes sensor calibrations unreliable and, since the density and velocity sensitivity coefficients are not equal, modal analysis techniques cannot be used to resolve the desired flow quantities.

The present paper reevaluates the use of hot-wire anemometry for obtaining fluctuating data in transonic flows in light of the recent hot-wire sensitivity calibrations of Rose and McDaid (ref. 5) and Mateer et al. (ref. 6) and the recent developments of hot-wire sensors (refs. 5,7) and anemometer systems. It is concluded that the above mentioned problems can be overcome and meaningful fluctuating measurements obtained in transonic flows. Using Behrens' hot-wire response correlations (ref. 8) with appropriate end loss corrections following Dewey (ref. 9) to obtain hot-wire sensitivities (validated by the direct calibration results of refs. 5 and 6), a parametric study is conducted to define a range of Reynolds numbers and hot-wire overheat ratios where the velocity sensitivity coefficients are independent of Mach number and equal to the density sensitivity coefficients. The relative magnitudes of the total temperature and velocity or density sensitivity coefficients are also investigated to determine a range of wire overheat ratios where the mass flux fluctuations can be measured directly in lieu of being obtained by modal analysis techniques. The relations required to deduce the fluctuating velocity, density, and shear stress from the measured quantities are then presented. Several types of the sensors described eliminate wire breakage, vibration, and strain gauging problems. Although these sensors cannot be used with constant current anemometers (these anemometers can only be used with sensors with well-defined time constants), commercial constant temperature anemometers are now available with adequate frequency response for transonic flows. Finally, several examples of velocity and density fluctuations and shear stress measurements obtained in transonic boundary layers are presented.

DISCUSSION

Hot-Wire Sensitivities

General Equations.- Following the principles set forth by Morkovin (ref. 3) and Kovasznay (ref. 4), the fluctuating voltage of a heated wire held normal to the flow can be expressed in terms of the fluctuations in velocity, density, and total temperature as:

$$\frac{E'}{\bar{E}} = S_\rho \frac{\rho'}{\bar{\rho}} + S_u \frac{u'}{\bar{u}} - S_{T_t} \frac{T_t'}{\bar{T}_t} \tag{1}$$

where the sensitivity coefficients are defined for constant temperature operation following Morkovin's terminology (ref. 3), as:

$$S_\rho = \frac{1}{2}\left(\frac{\partial \ln Nu_t}{\partial \ln Re_t} - \frac{1}{\tau_{wr}} \frac{\partial \ln \eta}{\partial \ln Re_t}\right) \tag{2}$$

$$S_u = S_\rho + \frac{1}{2\alpha}\left(\frac{\partial \ln Nu_t}{\partial \ln M} - \frac{1}{\tau_{wr}} \frac{\partial \ln \eta}{\partial \ln M}\right) \tag{3}$$

$$S_{T_t} = \frac{K}{2A'_w} + \frac{1}{2}\left(K - 1 - n_t\right) + m_t S_\rho + \frac{1}{2}\left(S_u - S_\rho\right) \tag{4}$$

According to Morkovin (ref. 3) for high-speed flows, $(M > 1.2)$ $S_u = S_\rho$ and equation 1 can be written as:

$$\frac{E'}{\bar{E}} = S_{\rho u} \frac{(\rho u)'}{\overline{\rho u}} - S_{T_t} \frac{T_t'}{\bar{T}_t} \tag{5}$$

where $S_{\rho u} = S_\rho$. Using the modal analysis techniques of Kovasznay (ref. 4) to solve equation 5, we can obtain the fluctuating physical variables $\langle(\rho u)'\rangle$, $\langle T_t'\rangle$ and $R_{(\rho u)T_t}$ by operating the wire at different overheat ratios (refs. 10-14). However, for transonic flow, the derivatives of the Nusselt number and recovery factor with Mach number are not zero and in general $S_u \neq S_\rho$. In fact, calculations given by Morkovin (ref. 3), based on hot-wire sensitivity data available at that time, indicate that the term $\partial \ln Nu_t/\partial \ln M$ is very large in the transonic flow regime at all wire overheats for a wide range of Reynolds numbers. This prohibits the use of the modal analysis technique and probably has discouraged the use of hot-wire anemometry in transonic flows for the past 20 years. In principle, if S_u and S_ρ could be determined for a hot-wire sensor then, by operating at six overheat ratios, the fluctuating quantities $\langle\rho'\rangle$, $\langle u'\rangle$ and $\langle T_t'\rangle$ and their cross-correlations could be obtained by inversion of a 6 x 6 matrix. This has been attempted (refs. 5,15) but without success, since the variation of S_u and S_ρ with wire temperature is small, and the resulting matrix is nearly singular.

Direct Calibration Measurements.- A recent investigation by Rose and McDaid (ref. 5), where the hot-wire sensitivity coefficients S_ρ and S_u were directly measured by varying density and velocity independently in the transonic flow regime, has shown that the ratio S_u/S_ρ is rather insensitive to changes in overheat, if the overheat is high. Summary of these newer data is given in figures 1, 2, and 3. The measurements were obtained using a specially designed 5-μm tungsten wire over a Mach number range from 0.3 to 1.2 and a wire Reynolds number range from 20 to 400. Within the accuracy of the measurements (±10%), the resulting sensitivity coeffients were found to be essentially independent of Mach and Reynolds numbers. These results are in apparent contradiction with previous predictions (ref. 3).

Subsequent to Morkovin's calculations (ref. 3), a significant amount of additional transonic hot-wire calibration data has been obtained. These data were successfully correlated by Behrens (ref. 8) in the form of recovery factor and Nusselt number as functions of Mach and Reynolds numbers. Using these correlations and the end loss corrections proposed by Dewey (ref. 9), we have evaluated the sensitivity equations for S_u and S_ρ (eqns. 2 and 3) for the test conditions of Rose and McDaid. The resulting values for S_ρ and S_u are compared with the data on figures 1, 2, and 3 for three wire length-to-diameter ratios at a wire Reynolds number and Mach number representing average values for the experimental data (Re_t = 100, M = 0.8). The actual wire ℓ/d was measured to be approximately 75. Comparing the experimental and calculated results, we see that the trends of the data and the predictions agree. The magnitude of the predicted sensitivities vary significantly with ℓ/d. Although not shown, they vary a similar amount over the experimental Reynolds number range, but show a negligible variation with Mach number for overheat ratios greater than 0.2. The calculated ratio S_u/S_ρ (fig. 3) is in excellent agreement with the experimental data, showing the two sensitivities are essentially equal for temperature overheat ratios greater than 0.4. It was also determined that the ratio S_u/S_ρ varies slowly with Re_t, M and ℓ/d for the range of experimental values (represented by the extremes on the bars on fig. 3). To predict adequately

S_ρ or S_u alone, direct calibration methods must be used since the effective ℓ/d of any sensor is a function of many variables and therefore may never be adequately known for predictive purposes. The apparent contradiction between the present results with the previous predictions by Morkovin (ref. 3) is due to the correlating equations used to predict the terms appearing in equation 3. Morkovin's correlations were based on limited data, which have since been updated by Behrens (ref. 8).

Additional evidence that Behrens' correlations can be used to predict the trends in recent calibration data is shown in figure 4, where the calculated sensitivities are compared with the experimental results from reference 6. The measurements were obtained by traversing a transonic boundary layer with a commercial cylindrical film sensor. The flow conditions varied from $M = 0.9$ at $Re_t = 180$ near the wall to $M = 1.4$ at $Re_t = 330$ at the outer edge of the boundary layer; it was assumed the sensor was responding to $S_{\rho u}$ (i.e., $S_u = S_\rho$). If S_u/S_ρ was a function of Mach number, one would expect significant variations of the measured sensitivity coefficient across the boundary layer. This was not the case. The data are compared with the present prediction method and excellent agreement is obtained in the sense that the predicted values of S_u and S_ρ are almost equal and independent of Mach and Reynolds numbers for the range of experimental data. The extremes on the bars represent the effect of varying Mach number from 0.9 to 1.4. (The small differences between the two calculated sensitivity coefficients are within the error limits one could expect when obtaining compressible turbulence measurements.) For these calculations, it was assumed that the sensor had no end losses, a reasonable assumption for film sensors. In this case, not only the trends of the data are predicted, but the absolute magnitude of the data are also predicted within 20%.

Parametric Investigation of S_u/S_ρ.- The previous comparisons (figs. 1-4) have shown that the trends in some of the recent transonic calibration data can be predicted. It is proposed that this calculation technique can be used to define a domain of hot-wire and flow variables where $S_u \approx S_\rho$. By testing within this domain, meaningful fluctuating measurements can be obtained at transonic speeds.

The sensor chosen as a baseline for the calculations was a tungsten wire with an $\ell/d = 100$ (similar to the probe used in ref. 5). The calculated ratio S_u/S_ρ for the baseline sensor, plotted as a function of wire Reynolds number and temperature overheat ratio, is shown on figure 5 for $M = 1.0$. For wire Reynolds numbers greater than 20 and temperature overheat ratios greater than 0.5, the sensitivity ratio is close to unity. For low Reynolds numbers, there appears to be no overheat ratio for which the velocity and density sensitivities are equal.

The variation of the calculated sensitivity ratio with Mach number is shown in figures 6 and 7. For a Reynolds number equal to one (fig. 6), the sensitivity ratio is only close to one for high Mach numbers, Reynolds numbers, and overheat ratios. Note that, for Mach numbers as high as 2.5, the sensitivity ratio for $\tau_{wr} = 0.1$ is only equal to 0.65. Therefore, for wire Reynolds numbers of order one, the modal analysis technique (ref. 4) for obtaining $\langle(\rho u)'\rangle$ and $\langle T_t'\rangle$ is open to question for Mach numbers as high as 2.5. Similar observations have also been made by McDaid (private communication). At a Reynolds number equal to 100 (fig. 7), the sensitivity ratio is approximately equal to one over the entire Mach number range, if $\tau_{wr} > 0.5$. At low overheats, the ratio reaches a maximum near $M = 1.1$ but for $M < 0.4$ or >2.5, the ratio is close to 1 over the entire range of wire overheat ratios.

The sensitivity ratio has also been calculated for various sensor materials and length-to-diameter ratios. These results are summarized in figures 8 and 9. Sensor material and ℓ/d do not influence the results for high values of Re_t but, for low values of Re_t, their influence is significant (due to large end loss corrections), and there is no region where $S_u \approx S_\rho$.

Summarizing these results, we have shown that, for wire temperature overheat ratios greater than 0.5 and Reynolds numbers greater than 20, one can assume $S_u \approx S_\rho \approx S_{\rho u}$ independent of Mach number, sensor material, or ℓ/d. However, no range of test conditions seems to exist where $S_u \approx S_\rho$ for all overheat ratios for transonic Mach numbers. Therefore, the modal analysis technique cannot be employed to determine the mass flux and total temperature fluctuations.

<u>Evaluation of $\langle(\rho u)'\rangle$ and $\langle T_t'\rangle$.</u>- Since the modal analysis technique cannot be employed at transonic speeds, an alternate approach must be used to separate the mass-flux and total temperature fluctuations. For many adiabatic flows of aerodynamic interest, the measured mean total temperature gradients are negligible and one can usually assume the total temperature fluctuations are also negligible. Thus, the mass-flux fluctuations can be measured directly. This assumption could be verified by operating the sensor at two overheat ratios (both high enough to ensure $S_u = S_\rho$) and evaluating the mass-flux fluctuations for each overheat, assuming zero total temperature fluctuations. If the two values agree, the assumption is valid, since the sensor sensitivity to total temperature is a function of overheat ratio.

If the total temperature fluctuations are not negligible, care must be taken to operate the sensor at overheat ratios where $S_{T_t} \gg S_{\rho u}$ or $S_{\rho u} \gg S_{T_t}$ when measuring total temperature or mass-flux fluctuations, respectively. Using equations 2 and 4, the ratio S_{T_t}/S_ρ has been calculated for a series of wire Reynolds numbers and wire materials and is shown as a function of overheat ratio on figures 10 and 11. (To evaluate A_w' in eq. 4, any possible variation in Nusselt number with overheat ratio was neglected. Although A_w' should be measured directly for each flow measurement, for most test conditions, the resulting error in S_{T_t} due to this assumption is small [ref. 3].) The results show that, for a sensor to be insensitive to total temperature fluctuations, the wire temperature overheat ratio must be greater than 1.5. For room temperature air (300°K), this requires a wire operating temperature greater than 750°K. These results are essentially independent of Re_t and sensor material and, although not shown, independent of Mach number and ℓ/d. At low overheats, the sensors are predominantly sensitive to total temperature fluctuations.

To measure $\langle T_t' \rangle$ directly, a constant current anemometer must be used because of frequency response limitations of constant temperature anemometers at low overheat ratios (ref. 12). For the direct measurement of $\langle(\rho u)'\rangle$ at high overheats, either system can be used. However, for transonic flows, where special wire or film sensors may have to be used to avoid wire breakage, only the constant temperature anemometer can be used (constant current anemometers can only be used for bare wires with well-defined time constants). Therefore, if wire breakage is a severe problem, total temperature fluctuations cannot be measured in transonic flows. Although equations 2, 3, and 4 are only valid for constant temperature operation, the calculated ratios S_u/S_ρ and S_{T_t}/S_ρ are valid for either system.

Equations for $\langle u' \rangle$, $\langle \rho' \rangle$ and $\overline{\rho}\,\overline{u'v'}$

Since the fluctuating quantities obtained from hot-wire measurements are $(\rho u)'$, v', T_t' and their cross-correlations, various assumptions must be employed to deduce the velocity and density fluctuations and Reynolds shear stress from the measured quantities. To calculate $\langle u' \rangle$ and $\langle \rho' \rangle$ from normal wire measurements of $\langle(\rho u)'\rangle$ and $\langle T_t' \rangle$, an assumption must be made concerning the fluctuating pressure and its correlations with velocity and temperature. The usual assumption is that $\langle p' \rangle$ is negligible. This assumption has been discussed at length (refs. 5,10,12,13,14) and should be valid for most transonic flows. The resulting equations (ref. 3) for $\langle u' \rangle$ and $\langle \rho' \rangle$ are:

$$\frac{\langle u' \rangle}{\overline{u}} = \frac{1}{\alpha + \beta}\left[\alpha^2\left(\frac{\langle(\rho u)'\rangle}{\overline{\rho u}}\right)^2 + \left(\frac{\langle T_t' \rangle}{\overline{T}_t}\right)^2 + 2\alpha\,\frac{\langle(\rho u)'\rangle}{\overline{\rho u}}\,\frac{\langle T_t' \rangle}{\overline{T}_t}\,R_{(\rho u)T_t}\right]^{1/2} \qquad (6)$$

$$\frac{\langle \rho' \rangle}{\overline{\rho}} = \frac{\langle T' \rangle}{\overline{T}} = \frac{1}{\alpha + \beta}\left[\beta^2\left(\frac{\langle(\rho u)'\rangle}{\overline{\rho u}}\right)^2 + \left(\frac{\langle T_t' \rangle}{\overline{T}_t}\right)^2 - 2\beta\,\frac{\langle(\rho u)'\rangle}{\overline{\rho u}}\,\frac{\langle T_t' \rangle}{\overline{T}_t}\,R_{(\rho u)T_t}\right]^{1/2} \qquad (7)$$

Since modal analysis techniques are invalid at low overheats in transonic flows, direct measurement techniques must be employed to determine $\langle T_t' \rangle$ and $R_{(\rho u)T_t}$. Direct measurements of $R_{(\rho u)T_t}$ have been obtained for supersonic flows and are described in reference 7. For cases where both the total temperature and pressure fluctuations are negligible, a single voltage reading at high overheat will yield both the normal velocity and density fluctuations.

To obtain the Reynolds shear stress from hot-wire measurements, the assumption that the pressure vertical-velocity correlation $\overline{p'v'}$ is negligible must be made (refs. 7,10). This assumption is less restrictive than that required for calculating $\langle u' \rangle$ or $\langle \rho' \rangle$ since $\overline{p'v'}$ can be negligible, although $\langle p' \rangle$ is not. The resulting expression (ref. 7) for shear stress is:

$$\bar{\rho}\,\overline{u'v'} = \frac{C_p \bar{T}}{C_p \bar{T} + \bar{u}^2} \left(\overline{(\rho u)'v'} + \frac{\bar{u}\,\bar{\rho}}{\bar{T}} \overline{T_t'v'} \right) \tag{8}$$

To measure $\overline{(\rho u)'v'}$ and $\overline{T_t'v'}$, a sensor inclined to the flow must be used (refs. 7,10,16). Techniques for the direct measurement of these quantities, which must be employed in transonic flows, are described in reference 7 using dual and triple sensor probes. An additional sensitivity coefficient (the sensitivity to flow angle) must also be determined. If the sensor is aligned 45° to the mean-flow direction, the sensitivity to flow angle is approximately equal to $S_{\rho u}$ for high overheats (refs. 7,16). The angle sensitivity of a particular sensor can also be measured directly by pitching the sensor in the flow (refs. 10,16).

Since the measurement of $\overline{T_t'v'}$ requires a special probe design (ref. 7), an alternate equation for obtaining Reynolds shear stress has been proposed (ref. 17), which makes the additional assumption of a known turbulent Prandtl number. The equation for shear stress in this case is

$$\bar{\rho}\,\overline{u'v'} = \left[1 + \frac{(\gamma - 1)\,M^2}{Pr_t} \left(1 - \frac{C_p\, d\bar{T}_t/dy}{\bar{u}\, d\bar{u}/dy} \right) \right]^{-1} \overline{(\rho u)'v'} \tag{9}$$

For adiabatic flow, this relation reduces to

$$\bar{\rho}\,\overline{u'v'} = \left[1 + \frac{(\gamma - 1)\,M^2}{Pr_t} \right]^{-1} \overline{(\rho u)'v'} \tag{10}$$

Note that this relationship requires no knowledge of the total temperature fluctuations.

Previous measurements (refs. 18-20) in subsonic and supersonic boundary layers have shown that the turbulent Prandtl number varies from 1.0 to 0.7

across a boundary layer except when close to the wall and at the outer edge. Equations 9 and 10 show that, as the Mach number increases, the Prandtl number assumption becomes more critical. For adiabatic flow, a 10% error in Prandtl number results in a 1% error in shear stress at Mach 0.5, a 3% error at Mach 1.0, and a 6% error at Mach 2.0. Considering the possible errors involved in obtaining transonic fluctuating data, these errors due to the Prandtl number assumption are not considered significant.

Sensors

Due to the high dynamic pressures incurred at transonic flows, wire breakage can be a severe problem. Rose and McDaid (ref. 5) have been successful in constructing tungsten wire probes with relatively small length-to-diameter ratios that survive in high Reynolds number transonic flows. Recall that ℓ/d has little effect on the ratio of sensitivities. An alternate solution to the wire breakage problem is to use specially constructed probes with the wires mounted on high-temperature ceramic wedges (ref. 7). With these probes, wire temperatures up to $1600^{\circ}K$ have been obtained, thus ensuring the wires can be operated so they are solely mass-flux sensitive. For some flows, the wire diameter required to ensure the wire Reynolds number is greater than 20 may be too large to obtain adequate frequency response. In this case, a commercial film probe could be used. The principle disadvantage to film sensors is their maximum operating temperature overheat ratio, which is 0.7. Although this is high enough to ensure $S_u = S_\rho$, it is not high enough to ensure the sensor is responding only to mass-flux fluctuations. Therefore, one must assume that the total temperature fluctuations are negligible, which should be verified for the flow field of interest by operating the sensors at two overheats. If significant total temperature fluctuations are present, film sensors cannot be used.

Since both the ceramic wedge and film sensors do not have well-defined time constants, constant temperature anemometers must be used to power these sensors. New commercial anemometers are now available with a special film bridge circuit which, at high overheat, attains a flat frequency response up to 150 kHz (ref. 7). This frequency response should be adequate for most experimental test flows at transonic speeds.

For inclined sensors, several additional complications must be considered. The principle problems are wire vibration and strain gauging effects which, when present, prohibit accurate measurements of the fluctuating vertical velocity and shear stress. To eliminate these problems, either wires mounted with substantial slack or wedge probes can be employed. However, both these solutions also offer certain disadvantages. For inclined wires with slack, the sensitivity to flow angle may become too small and inaccurate to obtain meaningful measurements. For wedge probes, especially those employing more than one sensor, thermal feedback problems can cause the probe sensitivities to be functions of frequency (ref. 21). This makes standard static calibration techniques invalid. For frequencies above 200 Hz, these effects are no longer present for most sensors, and the sensitivities are independent of frequency. Since, for most high-speed flows, only a small percentage of the total turbulent energy is contained in this low-frequency interval (refs. 6,7,10,14) dynamic calibration methods (calibrating the probe in a flow field previously measured with a single sensor) can be used to determine the probe sensitivities. Quantitative measurements can then be obtained using these sensitivities by neglecting the small errors due to the change in sensitivity at low frequency. For single cylindrical film sensors or single sharp-wedge film sensors, thermal feedback effects are minimized, and one can usually assume the sensitivities are independent of frequency. This should be verified for a particular sensor by independent calibration. As an alternative to calibrating the sensor, one can use a dual sensor to determine the ratio of the vertical-velocity to mass-flux fluctuations and the vertical velocity mass-flux correlation coefficient. These measurements are independent of the magnitude of the sensitivity coefficients and only require knowledge of the ratio $S_\phi/S_{\rho u}$. The mass flux fluctuations can then be determined using a single normal sensor.

Additional problems that occur when using crossed or dual wedge sensors include unequal sensitivities, spatial resolution, and mean flow and turbulence gradient effects. These effects are discussed by Sanborn (ref. 16). Provided the normalized mass-flux and vertical-velocity fluctuations are of the same order, sensitivity differences up to 10% between sensors will result in maximum errors of 10% in the measured quantities. Spatial resolution and flow gradient effects can be minimized by using miniature sensors and are usually unimportant except near solid surfaces (refs. 7,16).

Transonic Flow Measurements

Fluctuating measurements are presented for two boundary layers with edge Mach numbers approximately equal to 0.8. The first set of measurements (ref. 5) was obtained on a flat plate at $M_e = 0.8$, $\delta = 9$ cm, and $Re_\delta \approx 1.0\times10^6$. A normal tungsten wire was used to obtain $\langle(\rho u)'\rangle$. The second set of measurements was obtained by Mikulla (private communication) on the side wall of a pilot version of the Ames High Reynolds Number Channel at $M_e = 0.78$, $\delta = 3$ cm and $Re_\delta \approx .4\times10^6$. Both single and dual commercial film wedge sensors were used to obtain $\langle(\rho u)'\rangle$, $\langle v'\rangle$, $\langle w'\rangle$ and $\overline{(\rho u)'v'}$. A summary of the fluctuating intensities is given in figure 12. The sensors for both flows were operated at temperature overheat ratios of 0.7. This ensured that $S_u = S_\rho = S_{\rho u}$. The rms velocity and density fluctuations were calculated using equations 6 and 7, assuming negligible total temperature fluctuations. Note the excellent agreement between the measurements obtained in the two flows. The lateral and vertical velocity fluctuations were obtained using the dual film sensor to determine the ratios of lateral and vertical velocity to mass-flux fluctuations.

Reynolds shear stress measurements obtained for the Mach 0.78 flow are presented in figure 13. Equation 10 was used to calculate the shear stress with $Pr_t = 0.9$. The data are compared with integrated values obtained from measured values of mean velocity, mean temperature, and wall shear using the method of reference 20. The data are in excellent agreement with the integrated values for $y/\delta > .25$, which validates the Prandtl number and negligible pressure vertical-velocity correlation assumptions for the outer portion of this particular flow. Near the wall, the measurements are low, because of spatial resolution and interference effects which significantly influence the data in regions where the normal velocity gradients are large (ref. 16). The probe size was approximately 15% of the boundary-layer thickness. Smaller probes could be expected to minimize these effects.

CONCLUSION

The use of hot-wire anemometry for obtaining fluctuating data has been evaluated for transonic flows. Using hot-wire sensitivity correlations developed by Behrens (ref. 8) (validated by recent sensor calibration measurements) the density, velocity, and total temperature sensitivities have been calculated for a wide range of sensor and flow variables. These results have shown, for sensor Reynolds numbers above 20 and high sensor temperature overheat ratios, the velocity sensitivity coefficients are essentially independent of Mach number and equal to the density sensitivity coefficients. Therefore, direct measurements of the mass-flux fluctuations can be obtained, if the total temperature fluctuations are negligible, or the sensors are operated at overheat ratios sufficiently high to eliminate their sensitivity to total temperature fluctuations. Sensor calibration procedures are also simplified, since the sensitivity to density is easily obtained by placing the sensor in the free stream and varying the wind tunnel total pressure. From the measured mass-flux and vertical-velocity fluctuations, the fluctuating density and longitudinal velocity intensities and Reynolds shear stress can be deduced using assumptions appropriate to most transonic flows. Examples of these measurements for two boundary layers have been presented, showing the feasibility of hot-wire anemometry in transonic flows.

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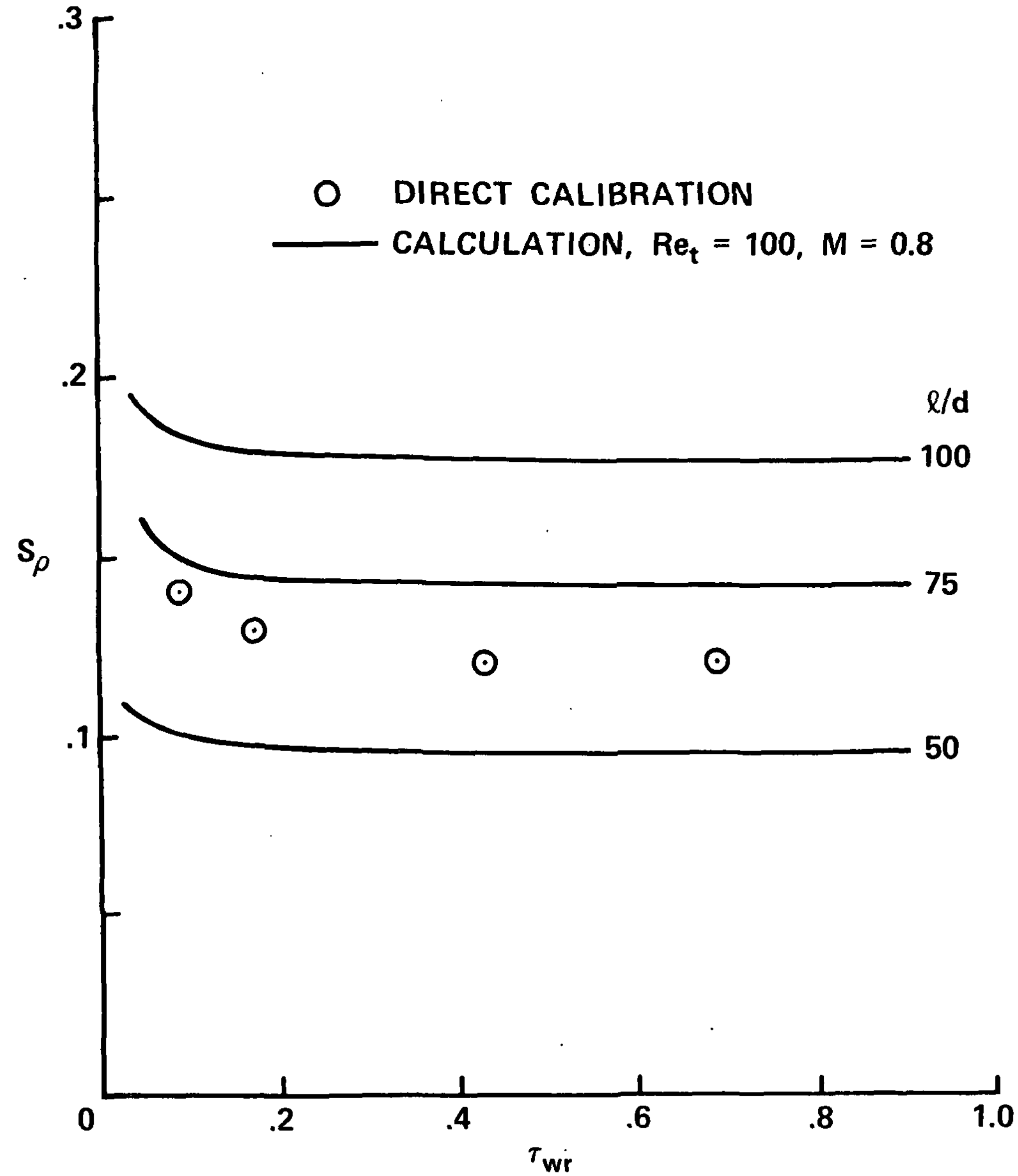

Fig. 1. Comparison of calculated and measured density sensitivities.

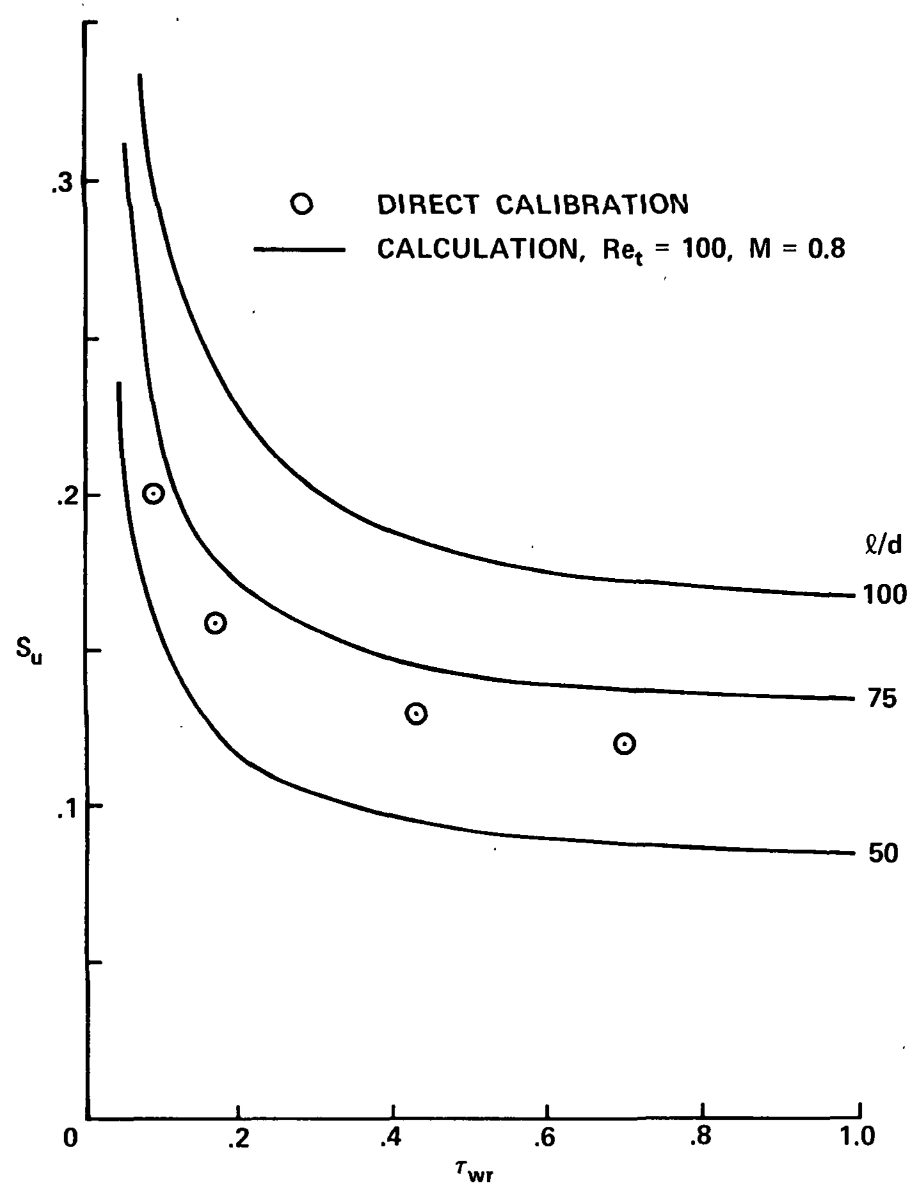

Fig. 2. Comparison of calculated and measured velocity sensitivities.

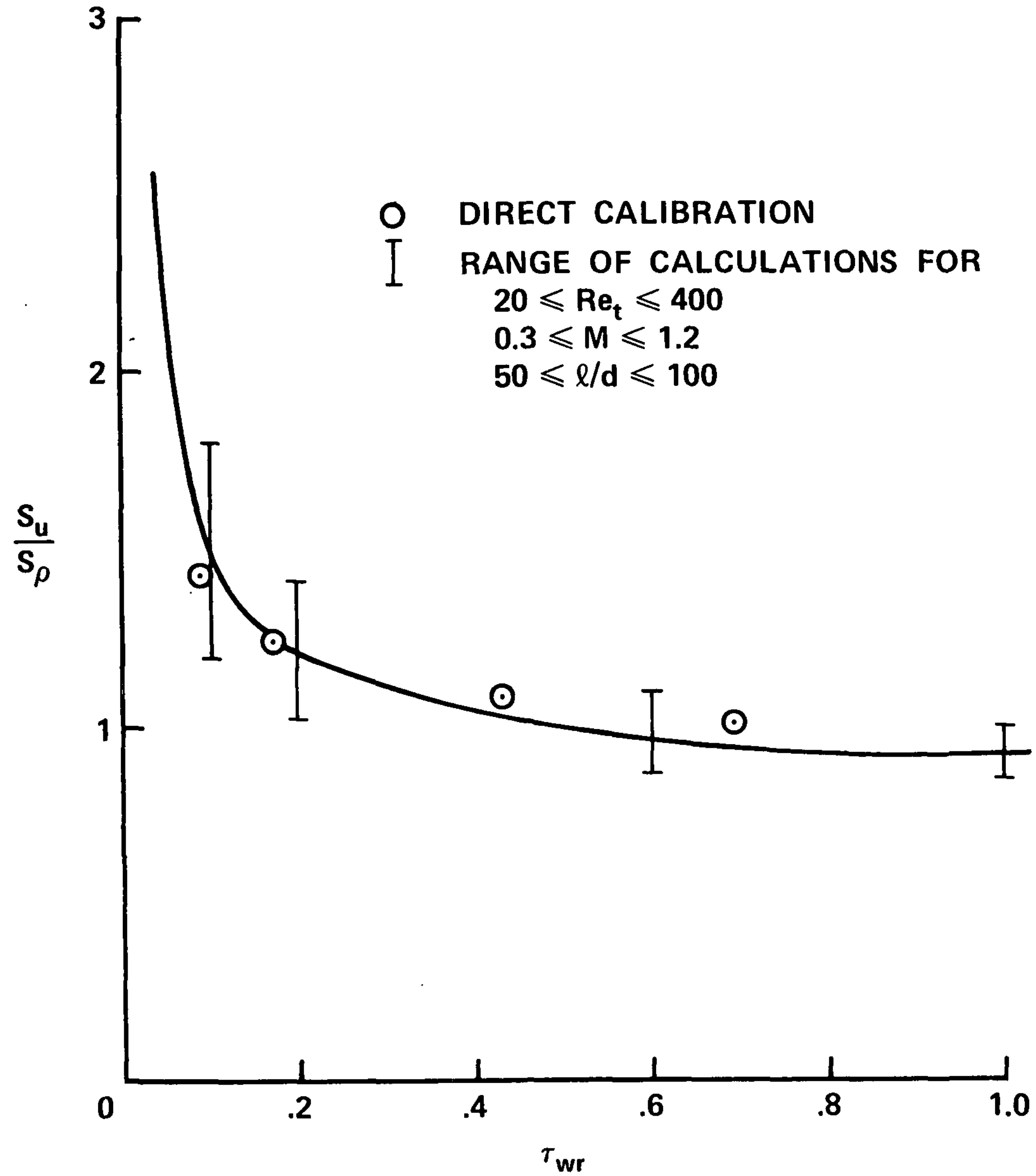

Fig. 3. Comparison of calculated and measured velocity-density sensitivity ratios.

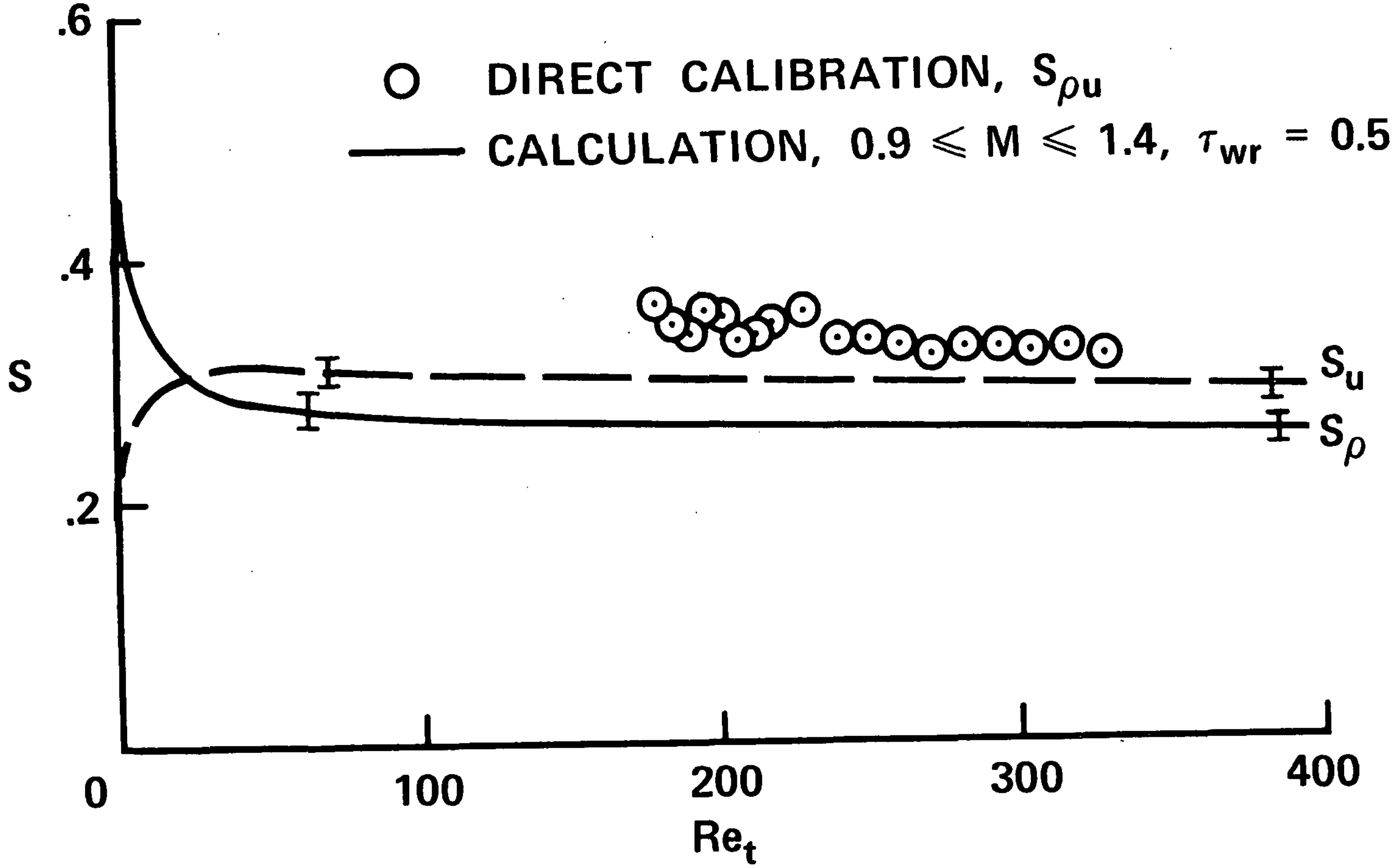

Fig. 4. Comparison of calculated density and velocity sensitivities and measured mass-flux sensitivity.

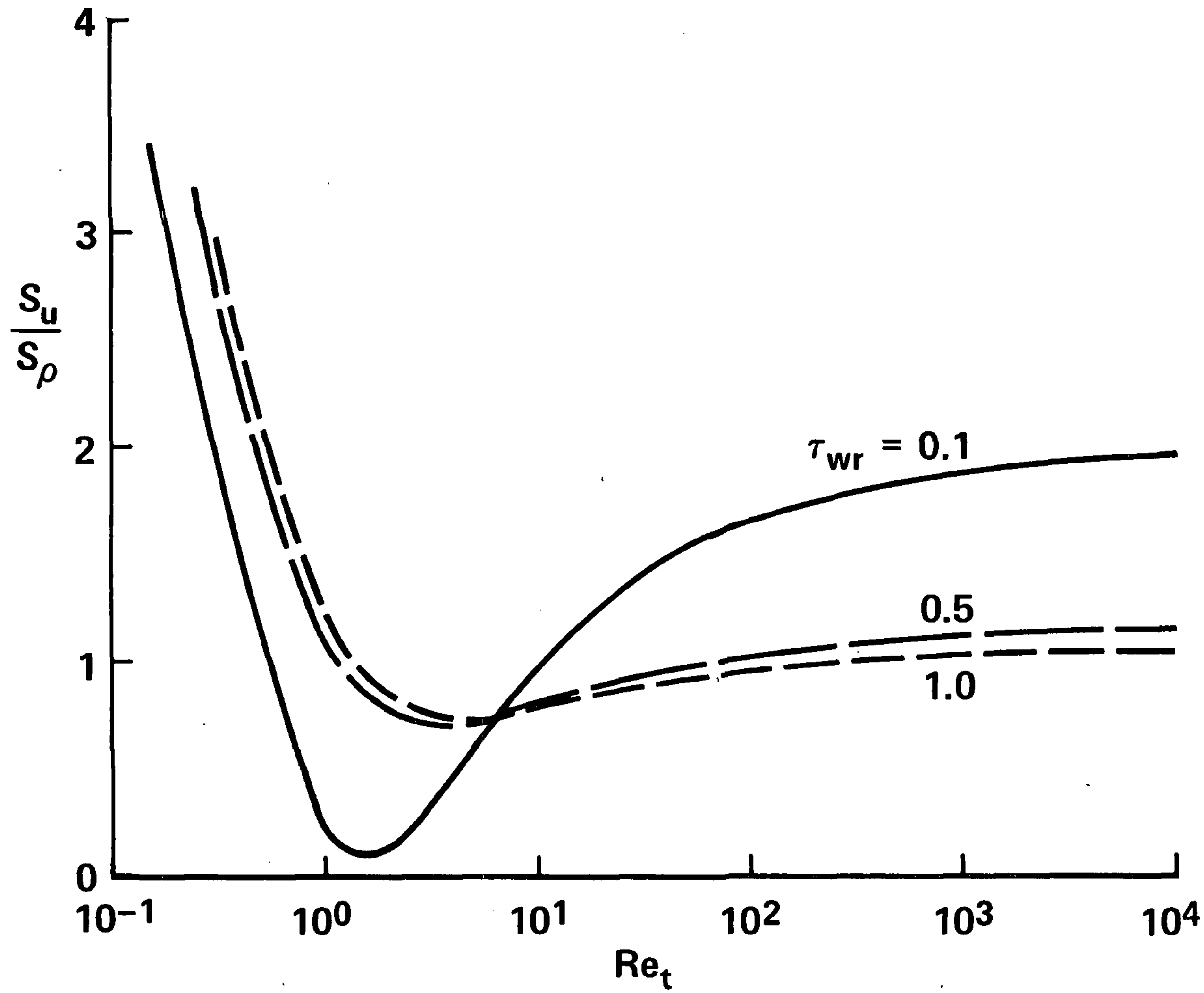

Fig. 5. Variation of velocity-density sensitivity ratio with Reynolds number and temperature overheat, M = 1.0, ℓ/d = 100, tungsten wire.

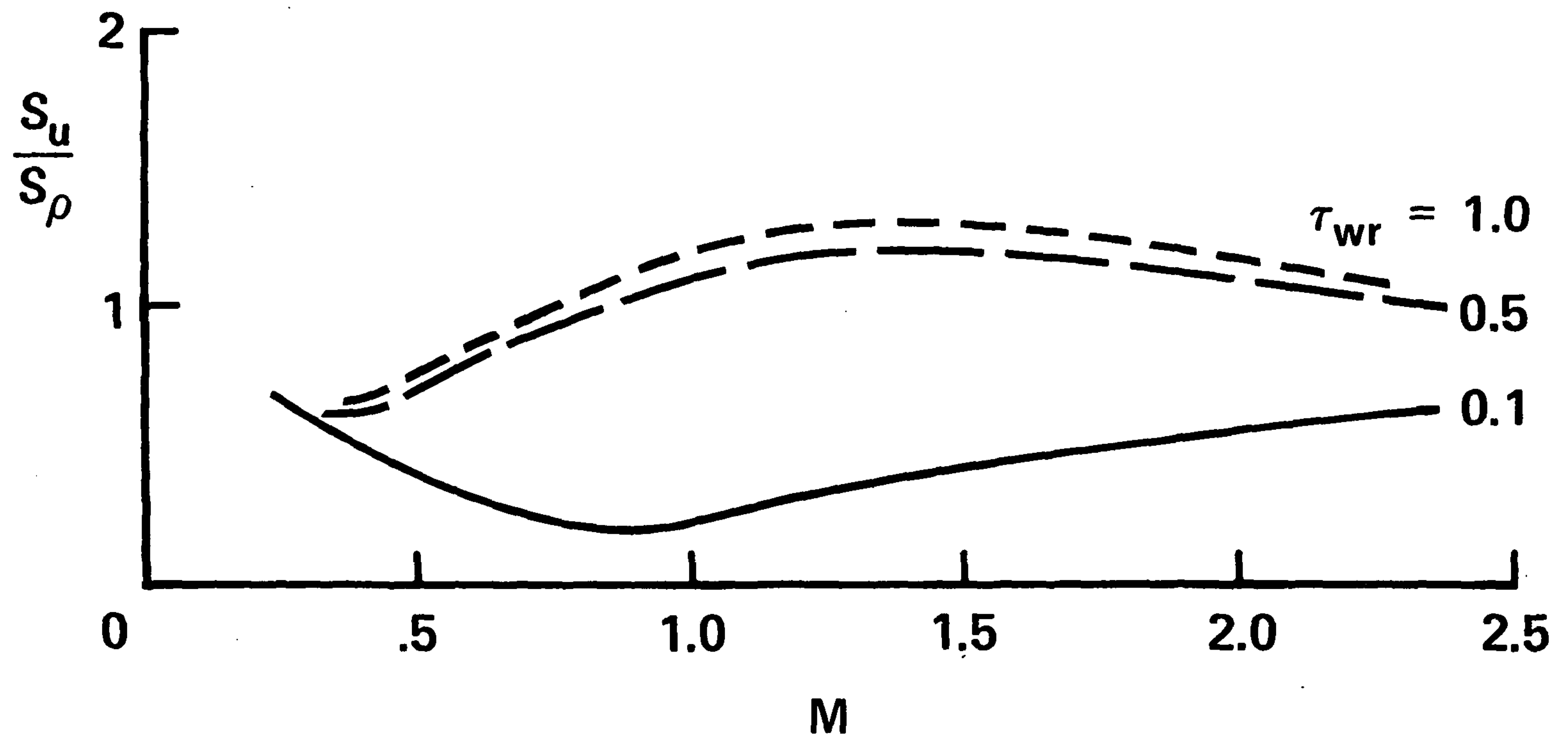

Fig. 6. Variation of velocity-density sensitivity ratio with Mach number and temperature overheat, $Re_t = 1.0$, $\ell/d = 100$, tungsten wire.

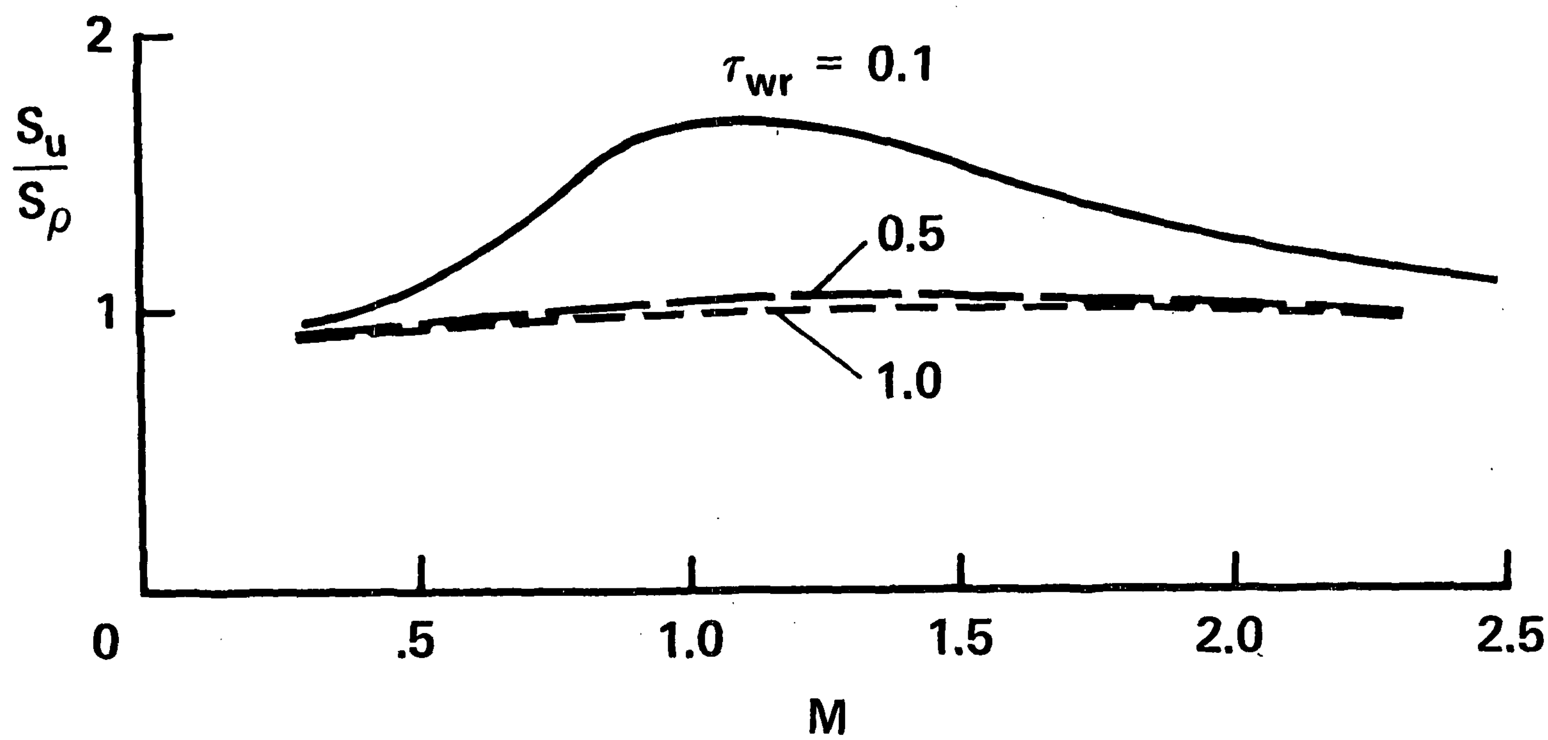

Fig. 7. Variation of velocity-density sensitivity ratio with Mach number and temperature overheat, Re_t = 100, ℓ/d = 100, tungsten wire.

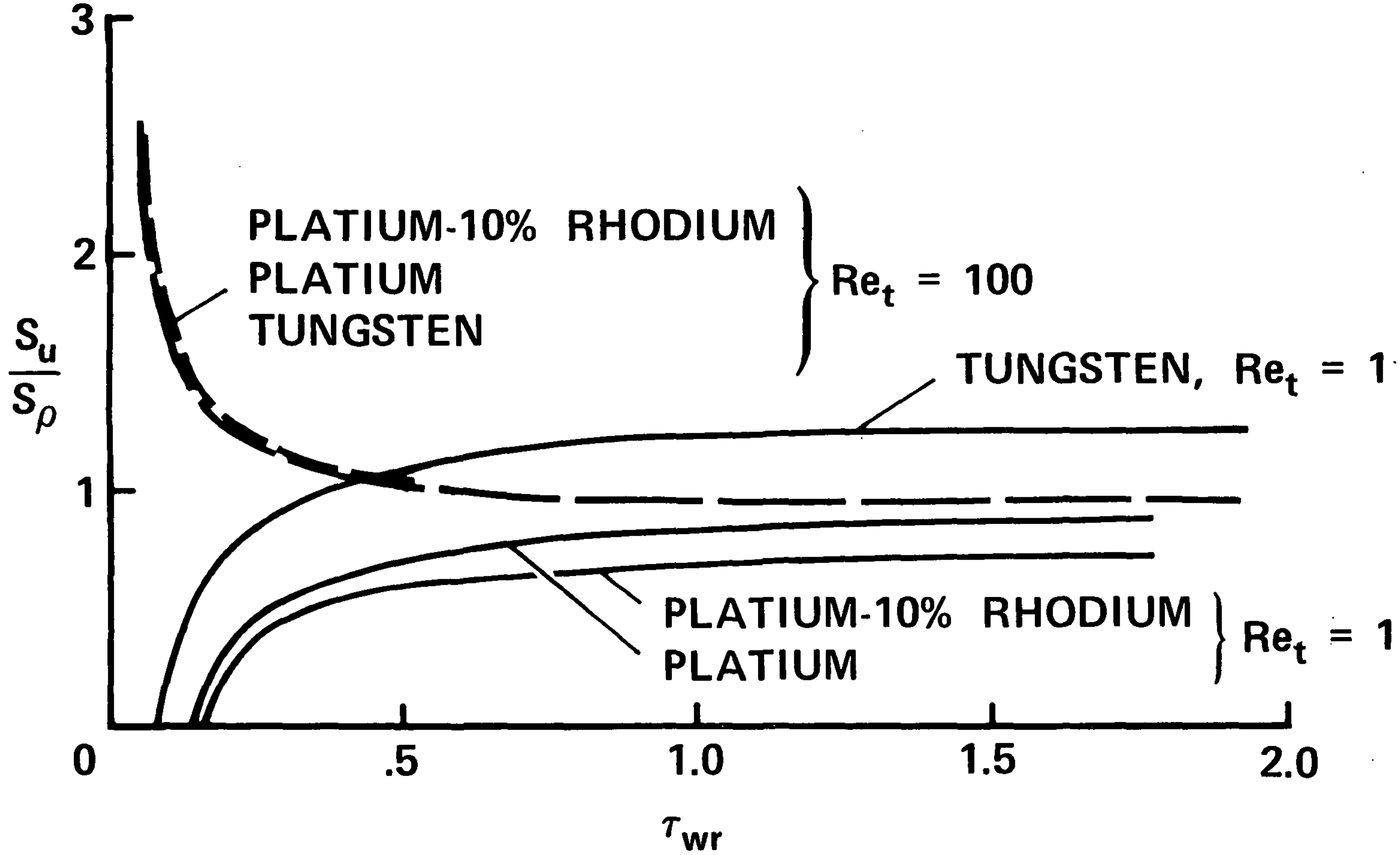

Fig. 8. Variation of velocity-density sensitivity ratio with wire material and temperature overheat, M = 1.0, ℓ/d = 100, Re_t = 1 and 100.

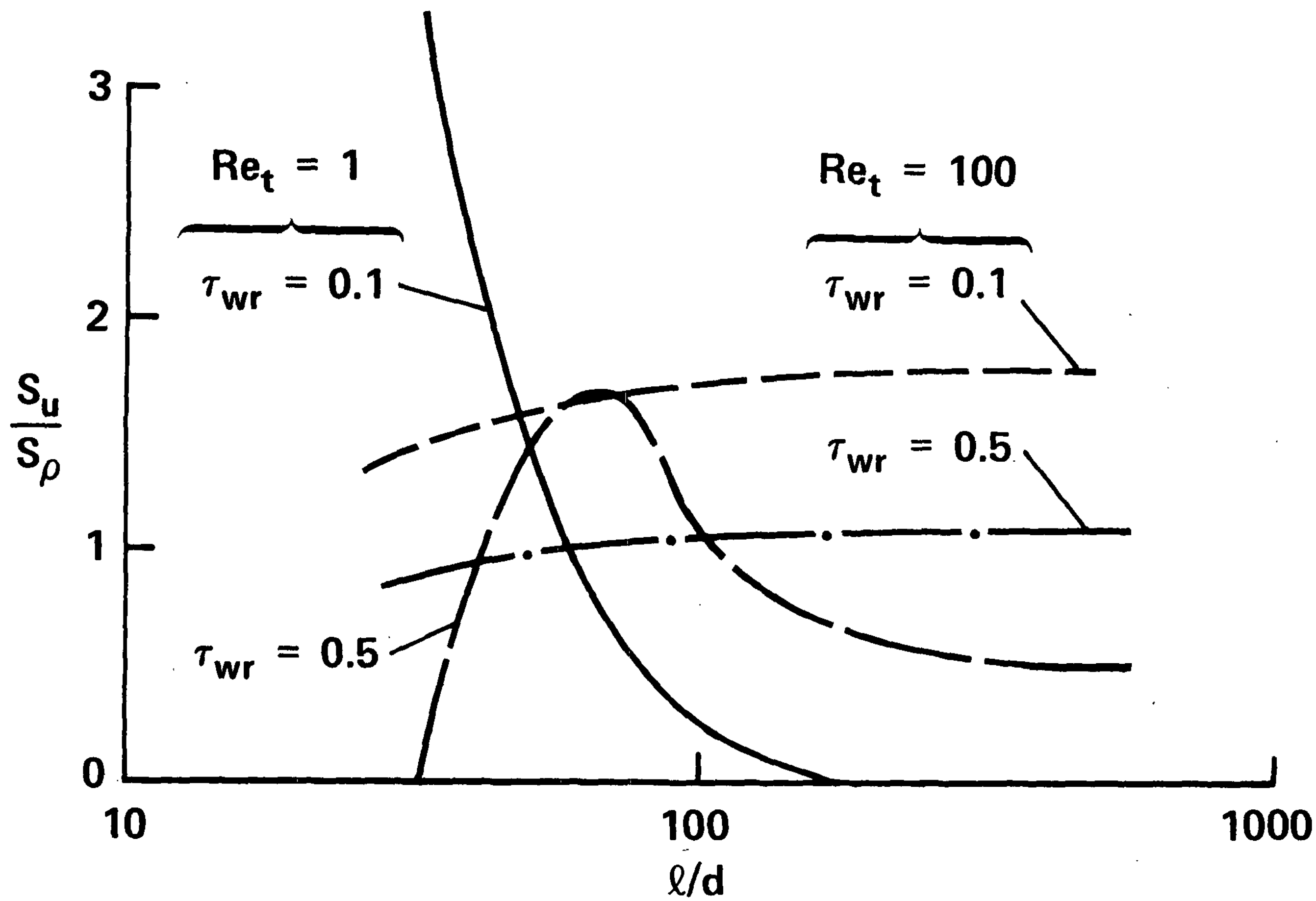

Fig. 9. Variation of velocity-density sensitivity ratio with ℓ/d and temperature overheat, M = 1.0, tungsten wire, Re_t = 1 and 100.

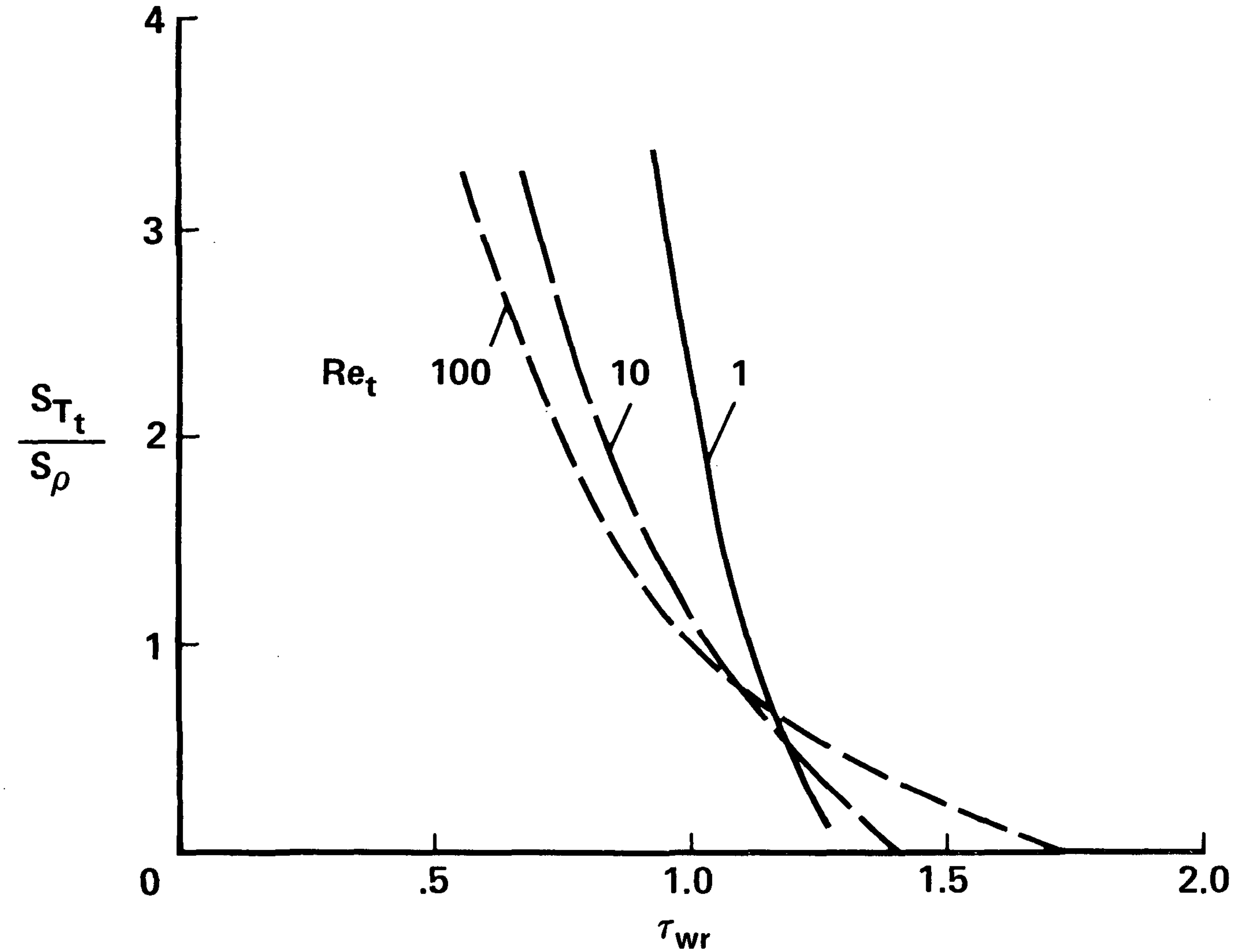

Fig. 10. Variation of total temperature-density sensitivity ratio with temperature overheat and Reynolds numbers, M = 1.0, ℓ/d = 100, tungsten wire.

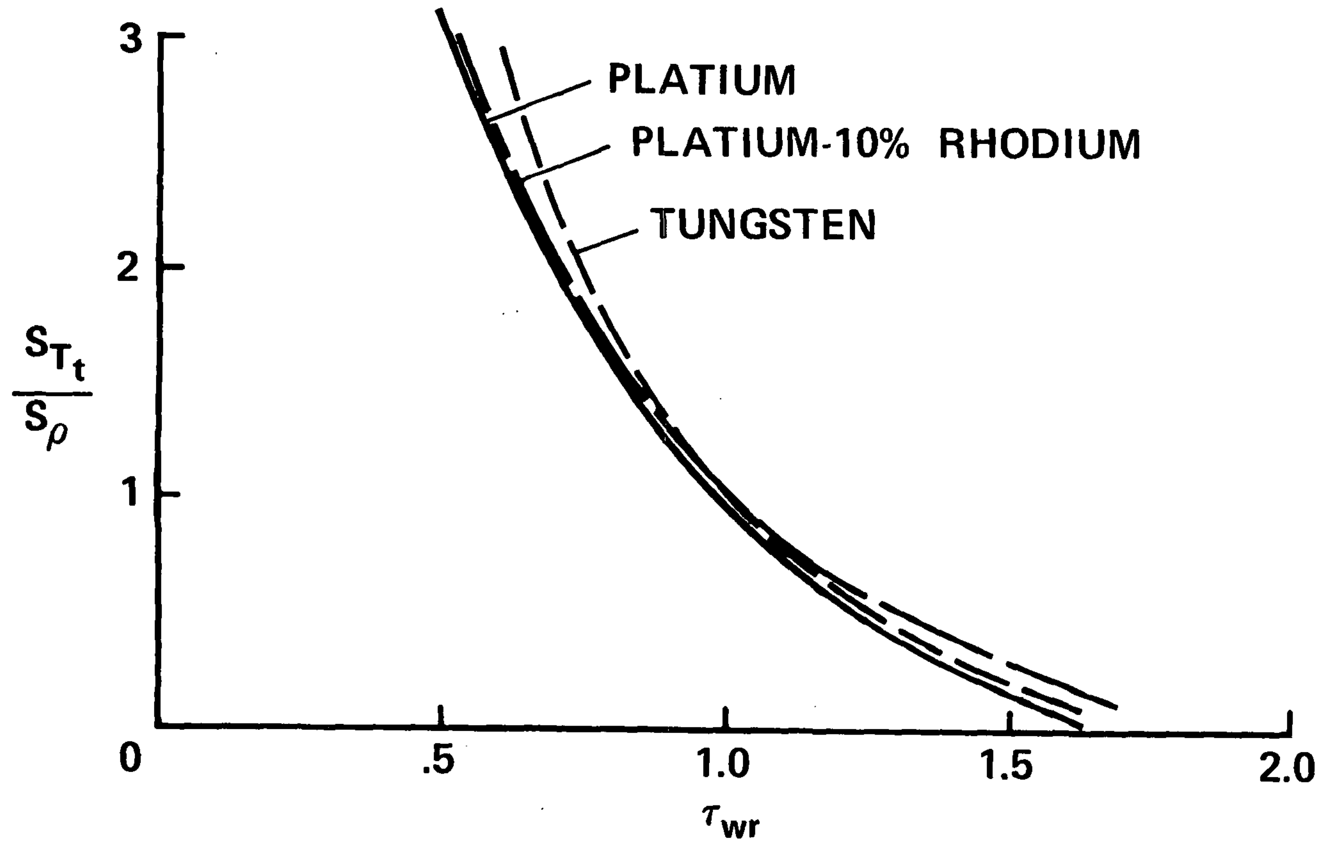

Fig. 11. Variation of total temperature-density sensitivity ratio with temperature overheat and wire material, M = 1.0, Re_t = 100, ℓ/d = 100.

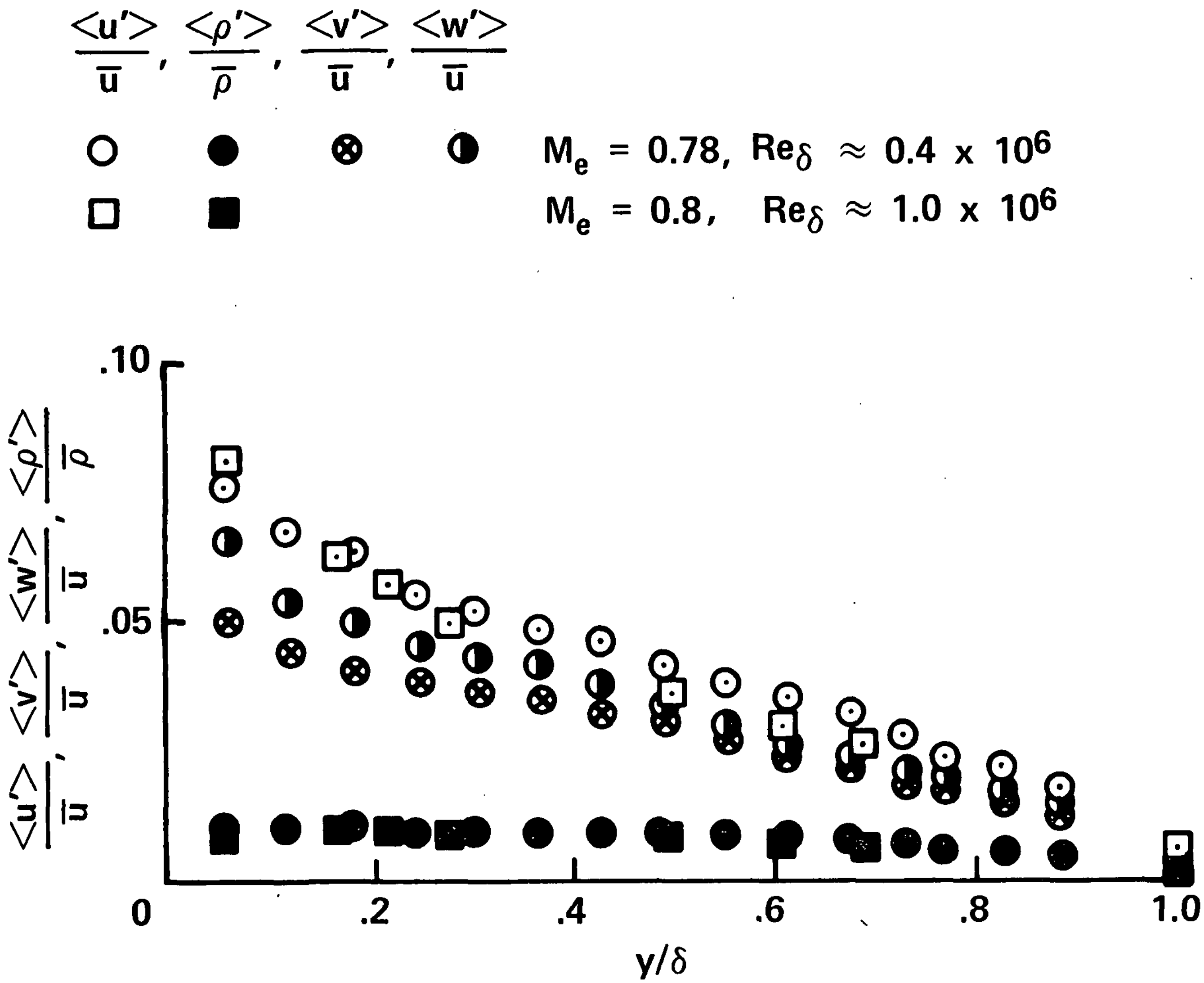

Fig. 12. Normalized rms velocity and density fluctuation distribution across the boundary layer.

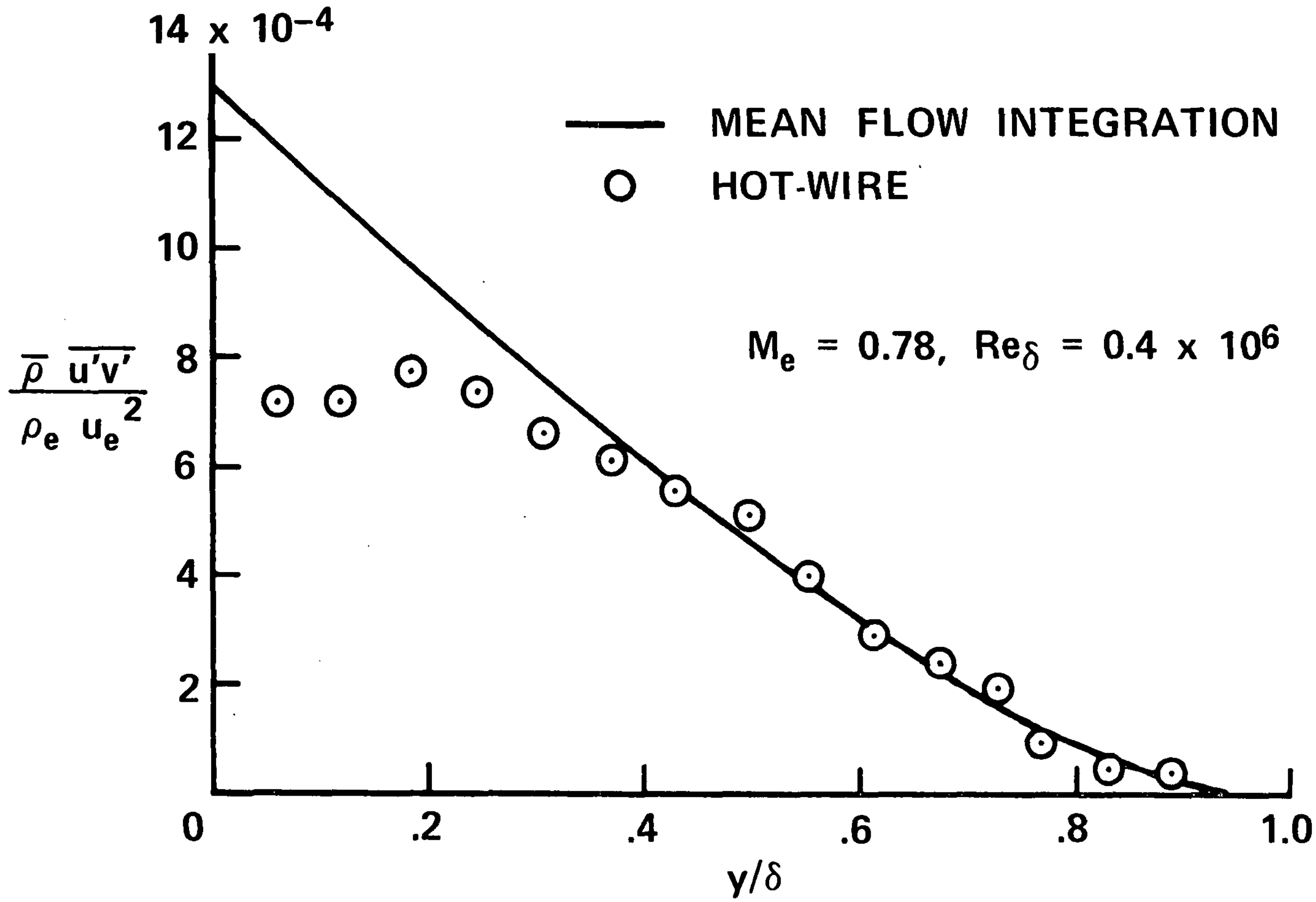

Fig. 13. Normalized Reynolds shear stress distribution across the boundary layer.