THEORETICAL AND EXPERIMENTAL STUDY
OF A NEW METHOD FOR PREDICTION
OF PROFILE DRAG OF AIRFOIL SECTIONS

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Prepared by
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Marietta, Ga. 30060
for Langley Research Center

NATIONAL AERONAUTICS AND SPACE ADMINISTRATION • WASHINGTON, D. C. • JUNE 1975
This report describes theoretical and experimental studies which were conducted for the purpose of developing a new generalized method for the prediction of profile drag of single component airfoil sections with sharp trailing edges. This method aims at solution for the flow in the wake from the airfoil trailing edge to the large distance in the downstream direction; the profile drag of the given airfoil section can then easily be obtained from the momentum balance once the shape of velocity profile at a large distance from the airfoil trailing edge has been computed. Computer program subroutines have been developed for the computation of the profile drag and flow in the airfoil wake on CDC6600 computer. The required inputs to the computer program consist of free stream conditions and the characteristics of the boundary layers at the airfoil trailing edge or at the point of incipient separation in the neighborhood of airfoil trailing edge. The method described in this report is quite generalized and hence can be extended to the solution of the profile drag for multi-component airfoil sections.
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<tr>
<td>c or C</td>
<td>Airfoil chord</td>
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<td>$C_D$</td>
<td>Drag coefficient $\frac{\text{Drag}}{\frac{1}{2}\rho U^2 \cdot \text{c-span}}$</td>
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<td>$C_p$</td>
<td>Constant pressure specific heat or pressure coefficient $\left(P-P_\infty\right)/\frac{1}{2}\rho U^2$</td>
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<td>$C_L$</td>
<td>Lift coefficient $\left(\frac{\text{Lift}}{\frac{1}{2}\rho U^2}\right)$</td>
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<td>$f(\eta)$ or $g(\xi)$</td>
<td>Function for defining the velocity profile similarity</td>
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<td>H</td>
<td>Form factor for airfoil surface or wake boundary layer $\delta^*/\theta$</td>
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<td>I</td>
<td>Hot wire current</td>
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<td>k</td>
<td>Curvature $1/R$ or thermal conductivity</td>
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<td>Value of similarity parameter at the upper or lower edge of the wake boundary layer</td>
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<td>l</td>
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<td>$q_\infty$</td>
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<td>Q</td>
<td>Convective heat flux defined by equation (111-3)</td>
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<td>R</td>
<td>Universal gas constant, or Radius of curvature of the stream lines in the wake flow, or Electrical resistance of hot wire</td>
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<td>t</td>
<td>Value of the maximum thickness of airfoil, or Thickness of the blunt trailing edge</td>
<td></td>
</tr>
<tr>
<td>T</td>
<td>Absolute temperature</td>
<td></td>
</tr>
<tr>
<td>Symbols</td>
<td>Definitions</td>
<td></td>
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<tr>
<td>---------</td>
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<td></td>
</tr>
<tr>
<td>$u, U$</td>
<td>X component of velocity</td>
<td></td>
</tr>
<tr>
<td>$U_e$</td>
<td>Velocity at the edge of the boundary layer over the airfoil surface or at the edges of airfoil wake boundary layer</td>
<td></td>
</tr>
<tr>
<td>$U_w$</td>
<td>Value of $u$ on the locus of minimum velocity in the airfoil wake</td>
<td></td>
</tr>
<tr>
<td>$U_\infty$</td>
<td>Free stream velocity</td>
<td></td>
</tr>
<tr>
<td>$u'$</td>
<td>Turbulent fluctuations in X-component velocity</td>
<td></td>
</tr>
<tr>
<td>$v, V$</td>
<td>Y-component of velocity</td>
<td></td>
</tr>
<tr>
<td>$v'$</td>
<td>Turbulent fluctuations in Y-component velocity</td>
<td></td>
</tr>
<tr>
<td>$x, y$</td>
<td>Rectangular coordinate system</td>
<td></td>
</tr>
<tr>
<td>$x'$</td>
<td>X Distance measured in the airfoil wake from the trailing edge of the airfoil</td>
<td></td>
</tr>
<tr>
<td>$X$</td>
<td>Transformed x-coordinates for pressure distribution at wake edges by equation (IV-6)</td>
<td></td>
</tr>
<tr>
<td>$Y_i$</td>
<td>Distances of the various characteristic loci in the wake measured from the reference line</td>
<td></td>
</tr>
<tr>
<td>$\alpha$</td>
<td>Angle of attack in degrees</td>
<td></td>
</tr>
<tr>
<td>$\beta$</td>
<td>Transformed x-coordinate for the pressure distribution along the locus of minimum velocity defined by equation (IV-8)</td>
<td></td>
</tr>
<tr>
<td>$\eta$ and $\xi$</td>
<td>Parameters for the similarity of velocity profiles</td>
<td></td>
</tr>
<tr>
<td>$\theta$</td>
<td>Momentum thickness for the boundary layer on the airfoil surface or for the wake boundary layer</td>
<td></td>
</tr>
<tr>
<td>$\delta$</td>
<td>Boundary layer thickness for the boundary layer on the airfoil surface or the boundary layer in airfoil wake</td>
<td></td>
</tr>
<tr>
<td>$\delta^*$</td>
<td>Displacement thickness for the boundary layer on the airfoil surface or for the wake boundary layer</td>
<td></td>
</tr>
<tr>
<td>$\gamma$</td>
<td>Parameter for the non-dimensional pressure at wake edge defined by equation (IV-5)</td>
<td></td>
</tr>
<tr>
<td>$\tau$ or $T$</td>
<td>Algebraic sum of shear stresses due to molecular viscosity and turbulent fluctuations</td>
<td></td>
</tr>
<tr>
<td>$\rho$</td>
<td>Density</td>
<td></td>
</tr>
<tr>
<td>$\mu$</td>
<td>Dynamic viscosity</td>
<td></td>
</tr>
</tbody>
</table>
Kinematic viscosity $= u/\rho$

Subscripts

$\infty$ Free stream values

e Edge of boundary layer

u Upper surface of airfoil or upper wake

L Lower surface of the airfoil or lower wake

T.E. Value at the trailing edge

A, B Value at station A or B, respectively

o or w Value at the wall
THEORETICAL AND EXPERIMENTAL STUDY OF A NEW METHOD
FOR PREDICTION OF PROFILE DRAG OF AIRFOIL SECTIONS

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Lockheed-Georgia Company

SUMMARY

This report describes theoretical and experimental studies which were conducted for the purpose of developing a new generalized method for the prediction of profile drag of single component airfoil sections. This method aims at solution for the flow in the wake from the airfoil trailing edge to the large distance in the downstream direction; the profile drag of the given airfoil section can then easily be obtained from the momentum balance once the shape of velocity profile at a large distance from the airfoil trailing edge has been computed. Computer program subroutines have been developed for the computation of the profile drag and flow in the airfoil wake on UNIVAC 1108 computer. The required inputs to the computer program consist of free stream conditions and the characteristics of the boundary layers at the airfoil trailing edge or at the point of incipient separation in the neighborhood of airfoil trailing edge. The method described in this report is quite generalized and hence can be extended to the solution of the profile drag for multi-component airfoil sections.

Experimental measurements of velocity profiles and static pressure distribution in the wake of sharp trailing edge test airfoil were obtained by use of a pitot static tube. Shear stress profiles in the airfoil wake were computed by the indirect method. A hot-wire anemometer was used for the measurements of velocity profiles in the wake of the blunt trailing edge test airfoil. The above measurements were obtained for a 15 percent thick modified NACA four digit airfoil section which was modified near the leading edge for the purpose of increasing the maximum lift coefficient.

A physical flow model for the wake flow was developed from the experimental data. This flow model is divided into regions and layers depending upon the characteristics of velocity profiles. Parameters for velocity profile similarity functions were developed from experimental measurements in different regions. Integral equations were derived for the solution of wake flow and these equations are coupled, ordinary nonlinear and nonhomogeneous differential equations. Parameters, such as shear and static pressures at the edges of various layers appear in the above equations as coupling terms. Functional representation of these parameters in terms of dependent variables, arranged in dimensionless groups, was accomplished with the help of the principle of local dynamic similarity.

The numerical solution of the above equations was accomplished by the modified Euler method with repeated iterations. Results of computations of the
computer program subroutines for the flow in the wake and profile drag were compared with experimental data for three airfoil sections. Reasonable agreement was obtained between measured and calculated flow quantities in the wake and profile drag corresponding to the occurrence of incipient separation on the upper surface of airfoil.

I. INTRODUCTION

With the recent energy crisis and shortage of fuel, it has become imperative to search for ways of reducing the drag of aircraft operating in the low and high subsonic speed regime, for their economical operation during cruise. The reduction in the drag of an aircraft, and hence saving in the fuel consumption, can be accomplished by proper design of the wing section. The effective design of wing sections, however, requires an accurate and reliable method for predicting profile drag. The trade-offs between aerodynamic and structural characteristics of airfoils must consider profile drag as a merit factor. A prediction method, which is based on theoretically sound principles consistent with real flow phenomena, can be used to evaluate candidate wing sections without resorting to the elaborate, expensive and time consuming wind tunnel tests. Moreover, the development of airfoils for special applications, such as minimum drag at specified lift conditions, requires a method with general applicability. The development of such a method was aimed during this study contract with NASA, Langley.

In order to estimate the drag of a body or to design a body for minimum drag, it is necessary to understand the origin and mechanism of the aerodynamic drag produced by a body. D'Alembert's paradox states that in an inviscid fluid a body can experience no drag. This can be proved relatively easily by use of the momentum theorem. Why then does a body experience drag in a real fluid? In the case of two-dimensional bodies, e.g. airfoil section at a positive angle of attack, the induced drag associated with lift is nonexistent, but profile drag or parasite drag of such bodies have a finite value depending upon free stream conditions and geometrical shape of two-dimensional bodies. The above mentioned profile drag of a two-dimensional body is composed of two parts, namely, the skin friction drag and the form or pressure drag. These parts may be of equal magnitude or the one may completely overshadow the other, depending upon the shape of the body. The skin friction drag is the result of the shearing stresses in the fluid as it passes over the surface of the body. The form drag results from the unbalance in normal pressure forces around the body due to the presence of attached or separated boundary layer around the surface of a body. These statements are exemplified by the use of Figure 1-1; this figure shows the cases of flat plate at 0° and 90° angles of attack and an airfoil at an angle of attack corresponding to the desired lift condition.

In Figure 1-1(a) the drag is entirely the result of skin friction, whereas in Figure 1-1(b) it is entirely form or pressure drag. If some means could be used to prevent the separation of the flow at the edges of the plate shown in Figure 1-1(b), the drag could be reduced to zero. The total or profile drag for the airfoil section of Figure 1-1(c) depends upon geometrical contour of the
(a) Flat Plate at 0° Angle of Attack

(b) Flat Plate at 90° Angle of Attack

(c) Airfoil at an Angle of Attack

Case (a) — Presence of Skin Friction Drag only
Case (b) — Presence of Pressure Drag only
Case (c) — Both Skin Friction and Pressure Drag constitute Total Drag

FIGURE I-1 — EXAMPLE SHOWING PRESENCE OF VARYING PROPORTION OF SKIN FRICTION AND PRESSURE DRAG CONSTITUENTS OF TOTAL PROFILE DRAG
body and free stream conditions such as Reynolds number, angle of attack, and Mach number. Combination of the above factors affect physical parameters, such as location and value of pressure peak on the upper surface of airfoil, existence of laminar boundary layer separation or transition, and conditions and growth of turbulent or laminar boundary layer on the upper and lower surfaces of the airfoil near its trailing edge. These physical parameters then determine the total profile drag of a given airfoil section and also the ratio of its constituents, namely, skin friction and pressure drag. The relation between pressure and friction drag of an axially-symmetric afterbody behind a long circular cylinder is of interest both from the point of view of practical application in design and the theoretical studies. Available experimental data for the drag coefficient, \( C_D \), for blunt and sharp afterbodies, which are shown in Figures 1-2(a) and 1-2(b), reveal an interesting phenomenon. Figure 1-2(a) shows the plots of measured drag coefficients, \( C_D \), versus \( LR/R_o \) for blunt and sharp afterbodies; in this Figure \( L_R \) denotes the length of the afterbody and \( R_o \) denotes the base radius of the afterbody. The experimental data shown in this figure reveal that blunt or thicker afterbody has a lower drag coefficient than the sharp afterbody. This result is quite surprising because it is known from available experimental data on airfoil sections (for example NACA four digit series) that thicker sections have higher values of profile drag than lower thickness ratio airfoil sections. The above-mentioned discrepancy of experimental results of Figure 1-2(a) can be explained with the aid of detailed analysis of experimental results shown in Figure 1-2(b). This figure shows the breakdown of the local total drag into local integrated values of skin friction drag and pressure drag. Values of integrals \( l_1 \) and \( l_2 \), which are shown plotted as a function of \( X/L_R \) in Figure 1-2(b) for both thicker and sharp afterbodies, represent local integrated values of skin friction and pressure drag, respectively. The algebraic sum of \( l_1 \) and \( l_2 \) at particular \( X \) location on the afterbody is equal to the total drag of afterbody up to that \( X \) location. The local integrated value of the pressure drag \( l_2 \) for blunt afterbody increases up to \( X/L_R = 0.75 \) and then starts decreasing up to the tail end because of better pressure recovery; this better pressure recovery is due to the absence of flow separation. For the sharp afterbody, however, the value of \( l_2 \) increases continuously up to the tail end because of flow separation at \( X/L_R = 0.8 \). Thus, the sum of \( l_1 \) and \( l_2 \) at the tail end, which represents the total drag coefficient, is higher for the sharp afterbody than for the blunt afterbody.

The total drag of an arbitrary airfoil section, at least in theory, can be computed from the knowledge of pressure distribution and airfoil geometry. From the knowledge of either experimental or calculated pressure distribution on the surface of the airfoil and the airfoil geometry, the pressure drag can be calculated from the \( C_P \) versus \( Z/C \) relationship of that airfoil, where \( Z/C \) is the nondimensional ordinate of the given airfoil section, the determination of the algebraic sum of the familiar thrust and drag loops gives the resultant pressure drag. The boundary layer characteristics and hence the skin friction drag can be calculated for the given pressure distribution by the use of pertinent boundary layer theories. In actual practice, however, it is extremely difficult to obtain accurate drag level by the above mentioned which is defined as the Direct Method. Slight errors in pressure distribution, while not significantly affecting the boundary layer characteristics, lift and pitching moment, can create errors in the thrust and drag loops that are seriously magnified when calculating pressure drag by taking the difference of these two loops. Hence,
FIGURE 1-2(a) - COMPARISON OF MEASURED DRAG COEFFICIENTS FOR BLUNT AND SHARP AFTERBODIES
\[ 1_l = \text{Local Integrated Value for Skin Friction Drag} \]
\[ = \int_0^{x/L_R} 2 \frac{L_R}{R_0} \frac{R}{R_0} \frac{\tau_w}{q} \frac{d(x)}{L_R} \]

\[ 1_2 = \text{Local Integrated Value for Pressure Drag} \]
\[ = \int_0^{x/L_R} 2 \frac{x}{L_R} C_p \frac{d(x)}{L_R} \]

**FIGURE 1-2(b) - BREAKDOWN OF THE TOTAL DRAG OF BLUNT AND SHARP AFTERBODIES INTO INTEGRATED VALUES OF SKIN FRICTION AND PRESSURE DRAG**
the use of the Direct Method is not recommended either for the purpose of data analysis or for the optimization study for the development of an airfoil configuration.

The profile or total drag of the airfoil section is usually calculated by some form of the Indirect Method. The calculation of the profile drag of a given airfoil section by use of the Indirect Method is dependent upon the ability of this method to reliably calculate the shape of the velocity profile at a very large distance downstream of the airfoil trailing edge. This very large downstream distance corresponds to the distance behind the trailing edge at which location static pressure is essentially constant and its value is equal to free stream static pressure. The drag of the given airfoil can then be calculated by subtracting the flow momentum corresponding to the above velocity profile at the large downstream distance from the corresponding momentum of the free stream air.

Use of the Indirect Method for the calculation of the profile drag, however, requires knowledge of the characteristics of the boundary layer on both upper and lower surfaces of the given airfoil in the vicinity of its trailing edge. These boundary layer characteristics, known as initial conditions, can be calculated, for example, for attached flow conditions by the use of methods in Reference 1.

The Indirect Method for calculations of profile drag, which is available up to the present time, is the Squire & Young's method of Reference 2. The simplified solution for the profile drag by the above method is obtained, however, at the sacrifice of making some assumptions which are inconsistent with physical reality. For example, logarithmic relation of velocity profile in wake, namely, the assumption of constancy of the ratio \( \frac{\log(U/\bar{U})}{H} \) along the wake, is contradicted by several experimental measurements in airfoil wakes. Moreover, the Squire & Young approach can be used to advantage in some instances, its limitations prevent reliable application to many problems of practical interest.

It is thus desirable to develop a generalized theoretical prediction method for the calculation of profile drag of an arbitrary airfoil, which is free from restrictions and limitations, such as discussed in the above paragraphs. Specifically, the method should be valid at high and low angles of attack and applicable to either single or multi-component airfoil. Specific objectives to be accomplished under the present studies are:

1. To obtain measurements of velocity profile and pressure distributions in the wake of a single component airfoil for the purpose of developing a physical wake flow model.
2. To derive the equations to provide a generalized mathematical model of airfoil wake and to develop a numerical method and a computer program for the solution of these equations.
3. To determine the validity of the method by correlation with measurements of the viscous flow in the airfoil wake and profile drag measurements.
II. THEORETICAL STUDY

II.1 General Discussion

The profile drag or total drag of the airfoil section is made up of two parts, namely, skin friction drag and pressure drag. Thus

$$C_D_{\text{profile}} = C_D_{\text{pressure}} + C_D_{\text{skin friction}}$$  \hspace{1cm} (II-1)

In the Direct Method, the total drag of the airfoil section is computed by evaluating the two components of the right-hand side of equation (II-1) separately, namely, (a) calculation of pressure drag and (b) skin friction drag. A brief description of this method was given in Section I. In the Indirect Method, the total drag is computed without evaluating the two components of the right-hand side of equation (II-1). This is accomplished from the knowledge of boundary layer parameters on the upper and lower surface in the vicinity of the trailing edge.

Figure II-1 illustrates the basic principle behind the Indirect Method for the purpose of calculating the profile drag of single or multi-component airfoil section. From the knowledge of the characteristics of the boundary layer on the upper and lower surfaces in the vicinity of the trailing edge, the Indirect Method is used for the purpose of calculating the velocity profile in the wake, at a large downstream distance behind the airfoil trailing edge, where static pressure distribution has stabilized to the freestream value. The expression for the profile drag of the two-dimensional airfoil section can then be written as

$$\text{Total Drag} = \int_{-\infty}^{+\infty} U(U_\infty - u) \, dy$$  \hspace{1cm} (II-2)

where

- $s = \text{span of airfoil}$
- $u = \text{local velocity in the wake boundary layer}$
- $U_\infty = \text{freestream velocity}$
- $\rho = \text{density}$.

The drag of the airfoil can then be determined if it is possible to use known velocity profile characteristics at the trailing edge of the airfoil to calculate the far downstream wake velocity distribution, $U(x \to \infty, y)$, point 4, Figure II-1.

$$U(x \to \infty, y) = F_i(U(x = \text{T.E., } y), P_1, P_2, P_3, \ldots, P_n)$$  \hspace{1cm} (II-3)

where $P_i, i = 1 \ldots n$ are $n$ different parameters required for the functional representation of equation (II-3). For example, in the case of a single component airfoil with a blunt trailing edge and with boundary layer separation ahead of the trailing edge in its vicinity, $P_1$ may be free stream velocity, $P_2$ may be local outer flow velocity at separation, $P_3$ the trailing edge thickness and $P_4$
Total Profile Drag = \( \zeta U_\infty^2 \left[ \int_{-\infty}^{+\infty} u(y)(U_\infty - u)dy \right] \)
where \( u(y) \) = Velocity at Station \( y \)

\[ X = -\infty \quad \text{(Large Distance Upstream from Leading Edge)} \]
\[ P = P_\infty \]

\[ X = +\infty \quad \text{(Large Distance Downstream from Tailing Edge)} \]
\[ P = P_\infty \]

\( \alpha \) = Angle of Attack

**FIGURE II-1 - SCHEMATIC REPRESENTATION OF AIRFOIL WAKE FOR COMPUTATION OF PROFILE DRAG**
may be the distance between separation point and the trailing edge. Each pair of parameters are also related to each other through the general velocity distribution \( U(x, y) \) at any point in the wake. For example, if \( U_e(x) \) is the velocity at the edge of the boundary layer in the wake, and \( U_\infty \) is the free stream velocity, then it is found from experimental data that it is possible to express the functional relationship as follows

\[
U_e(x) = F(U_\infty, \bar{u}) \tag{11-4}
\]

where

\[
\bar{u} = \frac{1}{\delta} \int_0^\delta u \, dy = \text{average velocity in the wake boundary layer.}
\]

Thus, \( (n-1) \) auxiliary equations, such as equation (11-4), are required for the complete solution of equation (11-3).

In the case of a single component airfoil with a sharp trailing edge, which does not exhibit any separation ahead of the trailing edge, only two parameters in equation (11-3) need to be considered. These parameters are \( U_\infty \) and the pressure distribution or \( U_e(x) \) along the wake. Squire and Young proposed the following empirical relation for the purpose of obtaining an analytic expression for the drag of a single component airfoil, i.e.

\[
\frac{\log_e \left( \frac{U_\infty}{U_e(x)} \right)}{H(x) - 1} = \frac{\log_e \left( \frac{U_\infty}{U_e(T.E.)} \right)}{H_{T.E.} - 1} = \text{Constant} \tag{11-5}
\]

where

\[
H(x) = \text{wake boundary layer form factor}
\]

\[
U_e(x) = \text{velocity distribution at the outer edge of the wake.}
\]

By making use of equation (11-5) in the von Karman momentum integral equation, Squire and Young were able to derive an analytic expression for the total drag coefficient of a single component airfoil with a sharp trailing edge. This expression, which is theoretically valid only in the absence of boundary layer separation ahead of the airfoil trailing edge, is given by

\[
C_d = 2(\theta_u + \theta_L) \left( \frac{U_{eT.E.}^2}{U_\infty} \right)^{(H_{T.E.} + 5)/2} \tag{11-6}
\]

It may be pointed out here that the above simple analytic expression for drag coefficient is obtained at the sacrifice of making some simplifying assumptions which are inconsistent with physical reality. For example, experimental data in the wake behind airfoils indicate that the empirical relation of equation (11-5) used by Squire and Young does not hold. Figure 11-2 shows the plot of experimental data for the ratio \( \log_e(U_\infty/U_e) /(H-1) \) at various distances aft of the trailing edge for a Joukowsky airfoil. These results show that
FIGURE II-2 - PLOT OF H. B. SQUIRE'S PARAMETER IN THE WAKE OF JOUKOWSKI AIRFOIL
assumption implied by equation (11-5) in the Squire and Young method is unrealistic. Specifically, Squire and Young's approach is unable to deal directly with the following situations:

1. Blunt trailing edge airfoils.
2. Airfoils composed of two or more components.
3. High or low angles of attack when boundary layer thickness on the upper and lower surfaces of the trailing edges are very difficult.
4. Incipient boundary layer separation in the neighborhood of the trailing edge on either the upper or lower surface or both.

11.2 Description of the Present Theoretical Method

Figure 11-3 shows the curvilinear system of coordinates in which $x$ denotes the distance along the locus of minimum velocity and $y$ the distance normal to it. The radius of curvature of the lower edge of the wake will be denoted by $R_1$ and of an equidistance line by $R$. It is assumed in this theoretical development that $R_1$ is large in comparison with the width of wake, but not very large in comparison with typical wake layer width. Defining curvature $K$ as

$$K = \frac{1}{R_1} \tag{11-7}$$

then one has

$$\frac{R_1}{R} = \frac{1}{1 \pm Ky} \tag{11-8}$$

The complete Navier-Stokes equations for the orthogonal directions, when local curvature in flow is taken into account, can be written as follows:

\[
\begin{align*}
\frac{R_1}{R} \left[ u \frac{\partial u}{\partial x} + v \frac{\partial u}{\partial y} + \frac{1}{\rho} \frac{\partial P}{\partial x} \right] &= \nu \left[ \frac{R_1}{R^2} \frac{\partial^2 u}{\partial x^2} + \frac{\partial^2 u}{\partial y^2} - \frac{R_1^3}{R^3} \gamma \frac{\partial \left( \frac{1}{R_1} \right)}{\partial x} \frac{\partial u}{\partial x} + \frac{1}{R} \frac{\partial u}{\partial y} - \frac{1}{R^2} u + 2 \frac{R_1}{R^2} \frac{\partial v}{\partial x} + \frac{R_1^3}{R^3} \nu \frac{\partial \left( \frac{1}{R_1} \right)}{\partial x} \right] \\
&= X - \text{Momentum Equation}
\end{align*}
\]
FIGURE II-3 - SYSTEM OF CURVILINEAR COORDINATES
The equation of continuity, considering effects of curvature, can be written as

\[
\frac{R_1}{R} u \frac{\partial v}{\partial x} + v \frac{\partial v}{\partial y} - \frac{u^2}{R} + \frac{1}{\rho} \frac{\partial p}{\partial y} = \nu \left[ \frac{R_1^2}{R^2} \frac{\partial^2 v}{\partial x^2} + \frac{\partial^2 v}{\partial y^2} - \frac{R_1^3}{R^3} \gamma \frac{\partial}{\partial x} \left( \frac{1}{R_1} \right) \frac{\partial v}{\partial x} + \frac{1}{R} \frac{\partial v}{\partial y} - \frac{v}{R^3} \frac{R_1^3}{R^3} u \frac{\partial}{\partial x} \left( \frac{1}{R_1} \right) - 2 \frac{R_1}{R^2} \frac{\partial u}{\partial x} \right]
\]

(II-10)

The equation of continuity, considering effects of curvature, can be written as

\[
\frac{\partial u}{\partial y} + \frac{\partial}{\partial y} \left( \frac{R}{R_1} v \right) = 0
\]

(II-11)

By performing time averaging operation on the terms of equation (II-9) and (II-10), the equations for the mean turbulent flow in the wake behind the trailing edge of the airfoil can be derived. These equations can be further simplified from the consideration of the order of magnitude analysis. The following time averaged equations can be written after appropriate simplification for the presently considered wake flow.

\[
\bar{u} \frac{\partial \bar{u}}{\partial x} + \frac{R}{R_1} \bar{v} \frac{\partial \bar{u}}{\partial y} + \frac{1}{R_1} \frac{\partial \bar{v}}{\partial x} = - \frac{1}{\rho} \frac{\partial \bar{p}}{\partial x} + \frac{R}{R_1} \frac{1}{\rho} \frac{\partial \bar{T}_{xy}}{\partial y} + \frac{1}{R_1} \frac{1}{\rho} \bar{T}_{xy}
\]

(II-12)

\[
+ \frac{1}{\rho} \frac{\partial}{\partial y} \left( \frac{u^2}{R} \left( \frac{1}{1 + y/R} \right) - \frac{R}{R_1} \frac{\partial \bar{T}_{xy}}{\partial x} \right)
\]

(II-13)

\[
\frac{\partial \bar{u}}{\partial x} + \frac{\partial}{\partial y} \left( \frac{R}{R_1} \bar{v} \right) = 0
\]

(II-14)

In the above equations, terms of the order \( \delta/R \) and of higher magnitude are retained and the terms of the order smaller than \( \delta/R_1 \) are eliminated; here \( \delta \) is the characteristic width of the wake boundary layer. The shear stress \( T(x,y) \) which appear in above equations is the algebraic sum of laminar and turbulent shear stress contributions, i.e.

\[
T(x,y) = \mu \frac{\partial \bar{u}}{\partial y} - \rho \bar{u} \bar{v} \bar{v}^T.
\]

(II-15)

In the further derivation of equations for the wake flow the bar above qualities, such as \( u, v, p \), etc., will be omitted; however, it will be implied that they represent the time averaged value at any point in the flow behind airfoil wake.

Physical Flow Model: Figure 11-4 shows the physical flow model of the wake aft of blunt base multi-component airfoil. This flow model was derived from available experimental data. The success of predicting the complex viscous flow,
FIGURE 11-4 - PHYSICAL MODEL FOR THE VISCOUS FLOW IN THE WAKE OF SINGLE COMPONENT OR TWO-COMPONENT AIRFOILS (cont'd)
Zone $L_1$ = Pressure of Maximum Circulatory Flow

Zone $L_2$ = Rapid Increase of Pressure

Zone $L_3$ = Infinitesimal small distance downstream of point of confluence or downstream of Sharp Trailing Edge

Rapid decrease in pressure

FIGURE II-4

(Continued)
such as shown in Figure 11-4, for an arbitrary airfoil case by direct solution of simplified Navier-Stokes equation (II-12), (II-13) and (II-14) would be highly improbable, if not impossible. For this reason, the flow model of Figure 11-4 is divided into various regions and layers in which the flow behavior has certain physical characteristics. For example, experimental measurements in the channel flow suggest that Region I can be divided into three Zones L1, L2, and L3. In Zone L1, the maximum circulatory flow is present as the pressure is almost constant, whereas in Zone L2, pressure increases rapidly to near stagnation value at the point of confluence. In Zone L3, which is infinitesimally small, pressure decreases at a rapid rate and the changes in velocity profile shape are quite abrupt.

From the present experimental measurement in the wake of a sharp trailing edge airfoil, it is observed that velocity profiles in the individual layers of Region IV of Figure 11-4 become "similar" or "one parameter family" if appropriate parameters are chosen. If the characteristics of the flow are such that it is possible to derive similarity parameters for all layers in different regions of Figure 11-4, then sets of ordinary differential equations for this wake flow can be derived from the governing partial differential equation such as (II-12), (II-13), and (II-14). These sets of ordinary differential equations, however complex, can then be solved by presently available numerical methods for the solution of the flow in the wake of an arbitrary airfoil. The discussion of similarity parameters for velocity profiles and generalized physical parameters for shear stress and pressure distributions in the wake behind single component airfoil will be given in Section IV. However, in the following derivations of theoretical equations for wake flow behind thin trailing edge single component airfoil section, it will be assumed that such similarity or generalized physical parameters do exist for the considered viscous flow.

11.3 Theoretical Derivations

Equations for Zone L3 of Region I: As the presently considered case is for single component airfoil with the sharp trailing edge, Zones L1 and L2 are absent; solution for the flow is thus required for Zone L3 only of Region I for this case. Schematic representation for Zone L3 for single component airfoil is shown in Figure 11-5. Initial conditions for the boundary layer quantities on upper and lower surfaces of airfoil are specified at Section AA, shown in this figure; this Section AA corresponds to either trailing edge location or is situated at the point of incipient separation on the airfoil which is in the neighborhood of airfoil trailing edge. The Section BB, shown in Figure 11-5, corresponds to the end of Zone L3 of Region I. This Section BB is located at an infinitesimal distance from Section AA; however, due to rapid mixing of boundary layers of upper and lower surfaces of an airfoil in Zone L3, characteristic viscous flow quantities such as static pressure, displacement and momentum thickness and magnitude of minimum velocity \( U_w \) change at much faster rates in Zone L3 than in downstream regions of flow. A starting value of \( U_w \) is obtained by making a momentum balance at the trailing edge. The use of momentum integral equation in the following form is made:
Subscript $u$ corresponds to upper surface of airfoil.

Subscript $L$ corresponds to lower surface of airfoil.

Momentum Thickness $\theta$, Form Factor $H$, Boundary Layer Thickness $\delta$ and $U_e$ are specified at Station A-A.

FIGURE II-5 - SCHEMATIC REPRESENTATION FOR ZONE L-3 FOR SINGLE COMPONENT AIRFOIL
\[ \frac{d\theta}{dx} + (H+2) \frac{\theta}{U_e} \frac{dU_e}{dx} = 0. \] \hspace{1cm} (11-16)

The parameter and function for the similarity of velocity profiles in Zone L3 are assumed to have the following form, namely,

For Upper Half Wake:

\[ \eta_1 = \frac{y - y_2}{y_3 - y_2}; \quad f(\eta_1) = \frac{U_{eu} - U}{U_{eu} - U_{w_1}} \] \hspace{1cm} (11-17)

For Lower Half Wake:

\[ \eta_2 = \frac{y_2 - y}{y_2 - y_1}; \quad f(\eta_2) = \frac{U_{el} - U}{U_{el} - U_{w_1}} \] \hspace{1cm} (11-18)

By making use of equations (11-17) and (11-18) in the usual definitions for momentum and displacement thickness, the expressions for these quantities for upper and lower wakes in Zone L3 can be derived in terms of parameters of equations (11-17) and (11-18). Thus,

\[
\theta_{\text{Upper Wake}} = (y_3 - y_2) \left[ \frac{U_{W_1}}{U_{eu}} - 1 \right] \frac{1}{0} \int f(\eta_1) d\eta_1 + \left( \frac{U_{W_1}}{U_{eu}} - 1 \right)^2 \frac{1}{0} \int f(\eta_1)^2 d\eta_1 \] \hspace{1cm} (11-19)

\[
\theta_{\text{Lower Wake}} = (y_2 - y_1) \left[ \frac{U_{W_1}}{U_{el}} - 1 \right] \frac{1}{0} \int f(\eta_2) d\eta_2 + \left( \frac{U_{W_1}}{U_{el}} - 1 \right)^2 \frac{1}{0} \int f(\eta_2)^2 d\eta_2 \] \hspace{1cm} (11-20)

\[
\delta^{\text{Upper Wake}} = (y_3 - y_2) \left( 1 - \frac{U_{W_1}}{U_{eu}} \right) \frac{1}{0} \int f(\eta_1) d\eta_1 \] \hspace{1cm} (11-21)

\[
\delta^{\text{Lower Wake}} = (y_2 - y_1) \left( 1 - \frac{U_{W_1}}{U_{el}} \right) \frac{1}{0} \int f(\eta_2) d\eta_2 \] \hspace{1cm} (11-22)

By making use of equations (11-17) through (11-22) in equation (11-16) and performing needed mathematical manipulations, the following quadratic equation can be derived for computing the ratio \( \left( \frac{U_{W_1}}{U_{ea}} \right) \) at the end of Zone L3.

\[ A_1 \left( \frac{U_{W_1}}{U_{ea}} \right)^2 + A_2 \left( \frac{U_{W_1}}{U_{ea}} \right) + A_3 = 0. \] \hspace{1cm} (11-23)
Where the values of coefficients are determined from following equations,

\[ A_1 = -(y_3 - y_2) \left\{ \int_0^1 f^2(n_1)dn_1 \right\} - (y_2 - y_1) \left\{ \int_0^1 f^2(n_2)dn_2 \right\} \left( \frac{U_{eu}}{U_{eL}} \right)^2 \]

\[ \left\{ \left( U_{eB} - U_{eA} \right) \frac{1}{U_{eB}} \right\} \cdot (y_3 - y_2) \cdot \int_0^1 f^2(n_1)dn_1 \] \text{UPPER WAKE}

\[ \left\{ \left( U_{eB} - U_{eA} \right) \frac{1}{U_{eB}} \right\} \cdot (y_2 - y_1) \cdot \int_0^1 f^2(n_2)dn_2 \cdot \left( \frac{U_{eu}}{U_{eL}} \right)^2 \] \text{LOWER WAKE}

\[ A_2 = (y_3 - y_2) \left[ 2\left\{ \int_0^1 f^2(n_1)dn_1 \right\} - \left\{ \int_0^1 f(n_1)dn_1 \right\} \right] \]

\[ + (y_2 - y_1) \left[ 2\left\{ \int_0^1 f^2(n_2)dn_2 \right\} - \left\{ \int_0^1 f(n_2)dn_2 \right\} \right] \left( \frac{U_{eu}}{U_{eL}} \right) \]

\[ - \frac{1}{2} \left\{ \left( U_{eB} - U_{eA} \right) \cdot \frac{1}{U_{eB}} \right\} \cdot (y_3 - y_2) \cdot \int_0^1 f(n_1)dn_1 \] \text{UPPER WAKE}

\[ - \frac{1}{2} \left\{ \left( U_{eB} - U_{eA} \right) \cdot \frac{1}{U_{eB}} \right\} \cdot (y_2 - y_1) \left( \frac{U_{eu}}{U_{eL}} \right) \cdot \int_0^1 f(n_2)dn_2 \] \text{LOWER WAKE}

\[ + \left\{ \left( U_{eB} - U_{eA} \right) \cdot \frac{1}{U_{eB}} \right\} \cdot (y_3 - y_2) \cdot \left\{ 2\int_0^1 f^2(n_1)dn_1 - \int_0^1 f(n_1)dn_1 \right\} \] \text{UPPER WAKE}

\[ + \left\{ \left( U_{eB} - U_{eA} \right) \cdot \frac{1}{U_{eB}} \right\} \cdot (y_2 - y_1) \cdot \left\{ 2\int_0^1 f(n_1)dn_1 - \int_0^1 f(n_2)dn_2 \right\} \cdot \left( \frac{U_{eu}}{U_{eL}} \right) \] \text{LOWER WAKE}
and

\[ A_3 = (y_3 - y_2) \cdot \left[ \int_0^1 f(n_1) \, dn_1 - \int_0^1 f^2(n_1) \, dn_1 \right] \]

\[ + (y_2 - y_1) \cdot \left[ \int_0^1 f(n_2) \, dn_2 - \int_0^1 f^2(n_2) \, dn_2 \right] \]

\[- \theta \left| \begin{array}{cc} \text{T.E.} & \text{T.E.} \\ \text{UPPER} & \text{LOWER} \\ \text{SURFACE} & \text{SURFACE} \end{array} \right| \cdot \frac{\theta}{U_e} \left| \begin{array}{cc} \text{T.E.} & \text{T.E.} \\ \text{UPPER} & \text{LOWER} \\ \text{SURFACE} & \text{SURFACE} \end{array} \right| \cdot (U_{eB} - U_{eA}) \]

\[ + \frac{1}{2} \left( H_{\text{T.E.}} + 2 \right) \cdot \frac{\theta}{U_e} \left| \begin{array}{cc} \text{T.E.} & \text{T.E.} \\ \text{UPPER} & \text{LOWER} \\ \text{SURFACE} & \text{SURFACE} \end{array} \right| \cdot (U_{eB} - U_{eA}) \]

\[ + \frac{1}{2} \left( H_{\text{T.E.}} + 2 \right) \cdot \frac{\theta}{U_e} \left| \begin{array}{cc} \text{T.E.} & \text{T.E.} \\ \text{UPPER} & \text{LOWER} \\ \text{SURFACE} & \text{SURFACE} \end{array} \right| \cdot (U_{eB} - U_{eA}) \]

\[ + \frac{1}{2} \left[ (U_{eB} - U_{eA}) \cdot \frac{1}{U_{eB}} \right] \cdot \left( y_3 - y_2 \right) \cdot \left( \int_0^1 f(n_1) \, dn_1 \right) \]

\[ + \frac{1}{2} \left[ (U_{eB} - U_{eA}) \cdot \frac{1}{U_{eB}} \right] \cdot \left( y_2 - y_1 \right) \cdot \left( \int_0^1 f(n_2) \, dn_2 \right) \]

\[ + \left[ (U_{eB} - U_{eA}) \cdot \frac{1}{U_{eB}} \right] \cdot \left( y_3 - y_2 \right) \cdot \left[ \int_0^1 f(n_1) \, dn_1 - \int_0^1 f^2(n_1) \, dn_1 \right] \]

\[ + \left[ (U_{eB} - U_{eA}) \cdot \frac{1}{U_{eB}} \right] \cdot \left( y_2 - y_1 \right) \cdot \left[ \int_0^1 f(n_2) \, dn_2 - \int_0^1 f^2(n_2) \, dn_2 \right]. \]
The variation of the width of the upper and lower wakes in Zone L3 is expressed by the following growth rate equations,

\[
\frac{d}{dx} (y_3 - y_2) = C_{IU} \cdot \left( \frac{U_{W1}}{U_{eL}} \right) \left( 1 - \frac{U_{eL}}{U_{W1}} \right) \tag{11-24}
\]

and,

\[
\frac{d}{dx} (y_2 - y_1) = C_{IL} \cdot \left( \frac{U_{eU}}{U_{eL}} \right) \left( 1 - \frac{U_{eU}}{U_{eL}} \right) \left( \frac{U_{W1}}{U_{eL}} \right) \tag{11-25}
\]

where \(C_{IU}\) and \(C_{IL}\) are constants and initial values of \((y_3 - y_2)\) and \((y_2 - y_1)\) for equations (11-24) and (11-25) correspond to values of boundary layer thickness at Section AA on upper and lower surface of the airfoil, respectively. The implied assumptions for the validity of equations (11-24) and (11-25) are that growth rate of these layers is controlled by the transverse perturbation velocity and that the transverse perturbation velocity is proportional to the average gradients of velocity for upper and lower wakes in Zone L3. Simultaneous solutions of equations (11-23), (11-24), and (11-25) give the starter values for \((U_{W1}/U_{eL}), (y_3 - y_2)\) and \((y_2 - y_1)\) at the end of Zone L3 of Region I. In the case of single component airfoil, the Regions II and III of Figure 11-4 are not present; and, thus, these starter values form the initial conditions for Region IV in the wake behind single component airfoil.

Equations for Region IV: For single component airfoil, the Region IV extends from the end of Zone L3 of Region I for a distance very far downstream of the airfoil trailing edge where the pressure has stabilized to the free stream value. Experimental data of the velocity profiles for layers 8-5 and 1-8 in Region IV indicate that velocity profiles in these layers become similar if the similarity parameters and similarity functions are defined in the following manner:

For layer 1-8:

\[
\eta_3 = \frac{y_8 - y}{y_8 - y_1} \quad f(\eta_3) = \frac{U_{eL} - u}{U_{eL} - U_{W8}} \tag{11-26}
\]

where \(u = \text{velocity at any point } y \text{ in layer 1-8}\),
\(U_{eL} = \text{velocity at lower edge of wake boundary layer}\),
\(U_{W8} = \text{minimum velocity on the locus of } y_8(x)\),
\(y_{1C} = \text{distance } y \text{ in the layer 1-8 where } u = 1/2 (U_{W8} + U_{eL})\)
and for layer 8-5 define:

\[ \eta_4 = \frac{y - y_8}{y_{8C} - y_8}; \quad f(\eta_4) = \frac{U_{eu} - u}{U_{eu} - U_{8W}} \]  
(11-27)

where \( u \) = velocity at any point \( y \) in layer 8-5

\( U_{eu} \) = velocity at upper edge of wake boundary layer

\( y_{8C} \) = distance \( y \) in the layer 8-5 where \( u = 1/2 (U_{eu} + U_{8W}) \).

In the definition of similarity parameters \( \eta_3 \) and \( \eta_4 \), as given by equations (11-26) and (11-27), the distances \( y_{1C} \) and \( y_{8C} \) are used because it is difficult to determine the exact values of boundary layer thickness from measurements at large distances downstream of the airfoil trailing edge. The Euler equation at the upper edge of the wake can be written as:

\[ \frac{dP}{dx} = -\rho U_{eu} \frac{dU_{eu}}{dx} \]  
(11-28)

whereas for the lower edge of wake, the Euler equation is written as:

\[ \frac{dP}{dx} = -U_{el} \frac{dU_{el}}{dx} \]  
(11-29)

The expression for the transverse velocity \( V(y) \) at any point in the layer 1-8 or layer 8-5 can be derived from the continuity equation (11-11) as follows,

\[ V(y) \frac{R(y)}{R_1} = V(y_8) \frac{R_2}{R_1} - \int_{y_8}^{y} \frac{3u}{\partial} \, dy \]  
(11-30)

where \( R(y) \) = radius of curvature of stream line in wake boundary layer at any point \( y \)

\( R_2 \) = radius of curvature for the locus of minimum velocity \( y = y_8(x) \)

\( R_1 \) = radius of curvature for lower edge of wake boundary layer

\( V(y_8) \) = transverse velocity on the locus \( y = y_8(x) \) of minimum velocity \( U_8(x) \).

Applicable boundary layer conditions for the layer 8-5 and layer 1-8 are:

at \( y = y_5 \): \( u = U_{eu}, \, f(\eta_4) = 0, \, \eta_4 = K_{1u}, \, C_p = C_p(y_5), \, \tau = \tau(y_5) = 0, \)  
and \( \frac{1}{\rho} \frac{d}{dx} = -U_{eu} \frac{d}{dx} \)

at \( y = y_8 \): \( u = U_{8W}, \, f(\eta_4) = f(\eta_3) = 1, \, \eta_3 = \eta_4 = 0, \, C_p = C_p(y_8), \)  
and \( V(y) = V(y_8) \)
and at \( y = y_1 \):  \( u = U_eL \), \( f(\eta_3) = 0 \), \( \eta_3 = K_1L \)

\[
C_P = C_P(y_1), \quad \tau(\eta_3) = 0, \quad \text{and} \quad \frac{1}{\rho} \frac{d}{dx} \frac{P_eL}{u_eL} = -U_eL \frac{d}{dx} \frac{u_eL}{u_eL}. \tag{11-31}
\]

In order to obtain momentum integral equation for layer 8-5, individual terms of equation can be integrated from \( y = y_8 \) to \( y = y_5 \). During the mathematical manipulations, use is made of equations (11-30), (11-29), (11-26), boundary conditions (11-31) and Leibnitz's rule. Radius of curvature \( R \) for the wake flow boundary is assumed large compared to the thickness of boundary layer for the purpose of analytical simplification. Integration of the term

\[
\int_{y_5}^{y_8} \frac{1}{R_1} u v \, dy
\]

needs some mention at this time. In order to perform this integration, the information regarding variation of integrand in the layer 8-5 is necessary. Experimental data indicate similarity in velocity profile for longitudinal velocity \( u \); however, such generalized information regarding transverse component \( v \) is not available at the present time. Under this circumstance, the results of analytical solution for Tollmien's (Reference 3) plane turbulent source are used for the purpose of evaluating this integral. Figure 11-6(a) shows the schematic representation of Tollmien's plane turbulent source.

Tollmien investigated the velocity distribution in the free jet flow which is formed when the flow of fluid emanates through the narrow rectangular slit. The \( x \)-axis coincides with the axis of symmetry and the \( y \)-axis is perpendicular to \( x \)-axis at the exit of the slit. The results of analytical solution, obtained by Tollmien for the flow conditions of Figure 11-6(a) are used for the present purpose as follows:

\[
I_1 = \int_{y_8}^{y_5} \frac{1}{R_1} u v \, dy \tag{11-32}
\]

Let

\[
g_1(\xi) = \frac{U_{eu} - U}{U_{eu} - U_{w_8}}; \quad g_2(\xi) = \frac{V}{U_{eu} - U_{w_8}} \quad \text{and} \quad \xi = \frac{y - y_8}{y_{8C} - y_8} \tag{11-33}
\]

when \( y = y_8, \xi = 0, y = y_5, \xi = K_2u \).

Substitution of (11-33) in (11-32) and simplifying,
Narrow rectangular slit through which flow emanates

Stream Lines

\( \psi = 0 \) and \( u = U_m \)

Equal Velocity Lines i.e., \( \frac{u}{U_m} = \text{Constant} \)

FIGURE 11-6(a) - SCHEMATIC REPRESENTATION OF TOLLMIEN'S PLANE TURBULENT SOURCE
\( \xi \) and \( g_2(\xi) \) defined by Equation II-33.

**Figure II-6(b) - Plot of \( g_2(\xi) \) from Tollmien's Solution**
\[
I_1 = \frac{1}{RT} (U_{eu}) (U_{eu} - U_{W8}) (\gamma_{8C} - \gamma_8) \int_0^{K_{2u}} g_2(\xi) \, d\xi
\]

\[
- \frac{1}{RT} (U_{eu} - U_{W8})^2 (\gamma_{8C} - \gamma_8) \int_0^{K_{2u}} g_1(\xi) g_2(\xi) \, d\xi \quad \text{(11-34)}
\]

Figures 11-6(b) and 11-7 show the plots \(g_2(\xi)\) and \(g_1(\xi)\). \(g_2(\xi)\) versus \(\xi\); these curves are obtained from theoretical results for Tollmien's plane turbulent source solution. From the above figures, the values of integrals can be written as:

\[
\int_0^{K_{2u}} g_2(\xi) \, d\xi = -.0214 \quad \text{and} \quad \int_0^{K_{2u}} g_1(\xi) g_2(\xi) \, d\xi = .0065 \quad \text{(11-35)}
\]

Thus, the momentum integral equation for the layer 8-5 can be derived as

\[
- \frac{d}{dx} \left[ (\gamma_{8C} - \gamma_8) (U_{eu}) (U_{eu} - U_{W8}) \right] \{ \int_0^{K_{1u}} f(n_4) \, dn_4 \}
\]

\[
+ \frac{d}{dx} \left[ (\gamma_{8C} - \gamma_8) (U_{eu} - U_{W8})^2 \right] \{ \int_0^{K_{1u}} f^2(n_4) \, dn_4 \}
\]

\[
- \left( \frac{dy_8}{dx} \right) (U_{W8}) (U_{eu} - U_{W8}) + U_{eu} \frac{d}{dx} (\gamma_5 - \gamma_8)
\]

\[
- \frac{d}{dx} (\gamma_{8C} - \gamma_8) (U_{eu} - U_{W8}) \{ \int_0^{K_{1u}} f(n_4) \, dn_4 \} + V(\gamma_8) \frac{R_S}{RT} (U_{eu} - U_{W8})
\]

\[
+ \frac{1}{RT} (U_{eu}) (U_{eu} - U_{W8}) (\gamma_{8C} - \gamma_8) \{ \int_0^{K_{2u}} g_2(\xi) \, d\xi \}
\]

\[
- \frac{1}{RT} (U_{eu} - U_{W8}) (\gamma_{8C} - \gamma_8) \int_0^{K_{2u}} g_1(\xi) g_2(\xi) \, d\xi
\]
FIGURE II-7 - PLOT OF $g_1(\xi) \cdot g_2(\xi)$ FROM TOLLMIEN'S SOLUTION

$g_1(\xi) \cdot g_2(\xi)$ are defined by Equation II-33.
If we assume that the rate of growth of the layer 8-5 is controlled by the transverse perturbation velocity, and — as experimental evidence indicates — similarity of velocity profile for this layer, then this growth rate can be expressed by the following equation:

\[
\frac{d}{dx} (y_8 - y_{1c}) = \frac{1 - \frac{U_{W8}}{U_{eL}}}{1 + 3 \frac{U_{W8}}{U_{eL}}} \frac{U_{W8}}{U_{eL}}
\]  

(11-37)

where \( C_{2u} \) is an empirical constant.

In the manner similar to the derivation of equation (11-36), the momentum integral equation for the layer 1-8 can be derived as:

\[
- \frac{d}{dx} \left[ (y_8 - y_{1c}) \cdot (U_{eL} - U_{W8}) \right] \int_0^{K_{1L}} f(\eta_3) d\eta_3 \\
+ \frac{d}{dx} \left[ (y_8 - y_{1c}) \cdot (U_{eL} - U_{W8})^2 \right] \int_0^{K_{1L}} f^2(\eta_3) d\eta_3 \\
+ \left( \frac{dy_2}{dx} \right) (U_{W8}) (U_{eL} - U_{W8}) + U_{eL} \frac{d}{dx} (y_8 - y_{1c}) \\
- \left( \frac{d U_{eL}}{dx} \right) (y_8 - y_{1c}) (U_{eL} - U_{W8}) \left\{ \int_0^{K_{1L}} f(\eta_3) d\eta_3 + V(\eta_8) \frac{R_8}{R_1} (U_{eL} - U_{W8}) \right\} \\
+ \frac{1}{R_1} (U_{eL}) (U_{eL} - U_{W8}) (y_8 - y_{1c}) \{ \int_0^{K_{2L}} g_2(\xi) d\xi \}
\]
and $R_{1-8}$, appearing in equations (11-36) and (11-38) can be approximated as the arithmetic mean values of radii of curvature for the layers 8-5 and 1-8 respectively. The growth rate equation for the layer 1-8 can be written in a manner similar to that for layer 8-5, as follows:

$$-\frac{1}{R_{1}} (U_{eL} - U_{W8})^2 \left( y_8 - y_{1C} \right) \int_{0}^{K_{2L}} g_1(\xi)g_2(\xi)d\xi$$

$$+ \frac{U_{\infty}^2}{2} \frac{d}{dx} \left[ \int_{Y_1}^{Y_8} C_p dy \right] - \frac{U_{\infty}^2}{2} C_p(y_8) \frac{dy_8}{dx} + \frac{U_{\infty}^2}{2} C_p(y_1) \frac{dy_1}{dx}$$

$$= \frac{\bar{R}_{1-8}}{R_1} \left( \frac{\tau(y_8)}{\rho} - \frac{\tau(y_1)}{\rho} \right) + \frac{1}{R_1} \int_{Y_1}^{Y_8} \frac{1}{\rho} dy$$

(11-38)

$\bar{R}_{8-5}$ and $\bar{R}_{1-8}$, appearing in equations (11-36) and (11-38) can be approximated as the arithmetic mean values of radii of curvature for the layers 8-5 and 1-8 respectively. The growth rate equation for the layer 1-8 can be written in a manner similar to that for layer 8-5, as follows:

$$\frac{d}{dx} (y_5 - y_{1C}) = C_{2L} \frac{1 - \frac{U_{W8}}{U_{eL}}}{1 + 3 \frac{U_{W8}}{U_{eL}}}$$

(11-39)

where $C_{2L}$ is an empirical constant.

Thus, equations (11-36), (11-37), (11-38) and (11-39) are four equations for the simultaneous solution of four variables $y_1, y_2, y_3$ and $U_{W8}$ in the Region IV, which is shown in Figure 11-4. These equations can be arranged in a manner to form the Initial Value Problem. Various single-step or multi-step methods can be used for the solution of this initial value problem. The most commonly used methods are the single step Euler method, the single-step modified Euler method, the multi-step predictor corrector method, the Runge-Kutta method, and a few others. The choice of a method depends upon the particular problem and is governed by the desired accuracy, time of computation, core size available in a particular computer, and other factors.

Equations (11-23), (11-36), and (11-38) contain terms such as

$$\int_{0}^{K_1} f(n)dn, \quad \int_{0}^{K_1} f^2(n)dn, \quad \int_{0}^{K_2} g_1(\xi)g_2(\xi)d\xi,$$

shear stress terms, terms containing shear and pressure integrals and CP distributions at the edges of various layers. In order to be able to solve these equations, however, the values of the above quantities either have to be known a priori and/or more auxiliary equations are required which express the above...
quantities as the functional relationships in terms of dependent variables. As the viscous flow under consideration is turbulent wake flow, theoretical expressions for the above quantities are not available as in the case of laminar boundary layers. Recourse is then made to experimental measurements to obtain empirical expressions for the above parameters by the use of experimental data for the particular flow which is being investigated. This matter is further discussed in Sections III and IV.

III. EXPERIMENTAL WORK

In order to facilitate understanding of turbulent wake flow, which exists behind the trailing edge of airfoil model, for the purpose of developing an analytical model and also to check the validity of theoretical predictions, an experimental program was conducted in the research wind tunnel facility at Lockheed-Georgia Company. In this section, a brief description is given for airfoil model, the experimental facility, probes for measurements of velocity and static pressure profiles, and types of tests. Detailed description of instrumentation, side wall blowing requirements in tunnel working section, and data acquisition and reduction is given in the appendices.

III.1 Description of Airfoil Model

Airfoil model is 15 percent thick and is symmetrical airfoil section. It is basically the NASA four digit airfoil section with slight modification near the leading edge. Figure III-1 shows the geometry of the test airfoil configuration; the difference in geometry near the leading edge of the test airfoil from NACA 0015 is also indicated in this figure. Computer program of Reference 1 indicated the existence of laminar stall on the original NACA 0015 airfoil at a Reynolds number of approximately 1 million. The leading edge geometry for the test airfoil was arrived at by the use of the computer program of Reference 1 such that turbulent separation near the trailing edge was predicted before the occurrence of laminar stall prediction on this test airfoil. The above phenomena then makes it possible to conduct systematic studies for the flow in the wake behind the airfoil trailing edge.

Figure III-2 shows the comparison between predicted $C_L-\alpha$ curve of the test airfoil with experimental measurements. It can be seen from this figure that viscous prediction agrees quite well with experimental measurements up to an angle of attack of $\alpha = 9^\circ$ to $10^\circ$. Experimental measurements of boundary layer velocity profiles indicate that turbulent boundary layer separation on the upper surface of the test airfoil near the trailing edge appears for angles of attack $\alpha$ greater than $9^\circ$ to $10^\circ$. Theoretical methods which are used in Reference 1 for computations of aerodynamic characteristics of airfoils are not valid in the presence of separated flow on the airfoil surfaces. For this reason, theoretical predictions of lift coefficients differ from experimental measurements for angles of attack $\alpha$ greater than approximately $9^\circ$ to $10^\circ$. 

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FIGURE III-1 - GEOMETRY OF THE TEST AIRFOIL
FIGURE III-2 - PREDICTED $C_L - \alpha$ CURVE FOR TEST AIRFOIL AND COMPARISON WITH EXPERIMENTS
Pressure distribution for the test airfoil at various angles of attack are shown in Figures III-3(a), III-3(b), III-3(c) and III-3(d). Computed results for potential and viscous pressure distributions are shown in the above figures and experimentally measured pressures at various chordwise locations on test airfoils at various angles of attack are shown plotted for comparison purposes. Comparison shown in Figures III-3(a) and III-3(b) indicate that theoretical predictions agree quite well with experimental measurements for angles of attack $\alpha$ equal to $0^\circ$ and $8^\circ$; however, for angles of attack $\alpha$ of $12^\circ$ and $14^\circ$, agreement between computed and experimental pressure distributions is not so good because of the existence of turbulent boundary layer separation on the upper surface of the airfoil.

Figures III-4(a) and III-4(b) show the plots of computed boundary layer development on the upper surface of the test airfoil for angles of attack of $8^\circ$ and $12^\circ$, respectively; experimental measurements of boundary layer quantities on the airfoil surface aft of $X/C$ of 0.8 are also shown in these figures. For the case of angle of attack of $8^\circ$, shown in Figure III-4(a), incipient separation may be present aft of $X/C$ of 0.96 near the trailing edge of the airfoil (as indicated in this figure by the computed value of the turbulent boundary layer form factor $H$ which becomes greater than 2.0 near the trailing edge). In this case the computed values of boundary layer displacement and momentum thicknesses and form factor agree reasonably well with experimental data. However, when the angle of attack is $12^\circ$, the results of computations of boundary layer quantities do not agree very well with experimental measurements on the upper surface of the airfoil because of the presence of appreciable region of flow separation on it. The boundary layer separation takes place at $X/C \approx 0.8$ at an angle of attack of $12^\circ$ approximately, and for this reason measured boundary layer thicknesses are much larger than computed values aft of $X/C = 0.8$.

The model was mounted vertically in the working section of the tunnel spanning the 76.2 centimeters test section. The chord of the airfoil model is 29.2 centimeters. Total of 16 static pressure orifices are provided on the upper surface and 10 orifices are provided on the lower surface of the model along Butt Line 0.00, i.e. along the center span of the model. In addition, a total of 15 additional static pressure orifices are provided on the upper and lower surfaces at Butt Lines $\pm 3.00$ for checking the two dimensionality of the flow on the airfoil model. Table 3-1 gives coordinates and Table 3-2 shows the chordwise locations of the static pressure orifices on the model.

Forward of chordwise location $X/C$ of 0.7, the model is comprised of a central load-carrying beam covered by a contoured maple shell. The shell consists of a solid maple leading edge and upper and lower surface "skins". The leading edge is bolted to the front of the beam. Aft of $X/C$ of 0.7 the model is made of solid aluminum. A tolerance of $\pm 0.0127$ centimeters is maintained on the airfoil contour across the span.

### 3.2 Wind Tunnel Facility

Experiments for the studies of the wake flow behind the single component test airfoil were conducted in the Lockheed Research Wind Tunnel facility which
$M_\infty = 0.201; \alpha = 0^\circ; R_H = 1.4 \times 10^6$

---

Potential Flow Calculations
Viscous Calculations
Experiments, Upper Surface
Experiments, Lower Surface

**FIGURE III-3(a)** - COMPARISON OF PRESSURE DISTRIBUTIONS FOR THE TEST AIRFOIL (SHARP T.E.) AT AN ANGLE OF ATTACK OF $0^\circ$
Figure III-3(b) - Comparison of pressure distributions on test airfoil (sharp T.E.) at an angle of attack of 8°.
FIGURE III-3(c) - COMPARISON OF PRESSURE DISTRIBUTIONS ON TEST AIRFOIL (SHARP T.E.) AT AN ANGLE OF ATTACK OF 12°
FIGURE III-3(d) - COMPARISON OF PRESSURE DISTRIBUTIONS ON TEST AIRFOIL (SHARP T.E.) AT AN ANGLE OF ATTACK OF 14°
$M_{\infty} = 0.201; \alpha = 8.0^\circ; R_{\infty} = 1.4 \times 10^6$

- ○ ○ Experiment, Displacement Thickness $\delta^*/c$
- △ △ Experiment, Momentum Thickness $\theta'/c$
- ■ ■ Experiment, Form Factor $H$
- Computations

**FIGURE III-4(a) - BOUNDARY LAYER DEVELOPMENT ON THE UPPER SURFACE OF THE TEST AIRFOIL (SHARP T.E.) AT AN ANGLE OF ATTACK OF 8° AND COMPARISON WITH EXPERIMENTS**
$M_{\infty} = 0.200; \alpha = 12.0^\circ; R_n \approx 1.4 \times 10^6$

- Computations
- Experiments, Displacement Thickness $\delta/c$
- Experiments, Momentum Thickness $\theta/c$
- Experiments, Form Factor $H$

**Figure III-4(b) - Boundary layer development on the upper surface of the test airfoil (sharp T.E.) at an angle of attack of 12° and comparison with experiments**
Table 3-1 – COORDINATES OF THE TEST AIRFOIL SECTION (SYMMETRICAL AIRFOIL)

Airfoil Chord, C = 29.2 inches

<table>
<thead>
<tr>
<th>Station, Percent Chord</th>
<th>Airfoil Section Ordinate Percent Chord</th>
</tr>
</thead>
<tbody>
<tr>
<td>X/C x 100</td>
<td>Z/C x 100</td>
</tr>
<tr>
<td>0.00</td>
<td>0.00</td>
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<tr>
<td>0.23</td>
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<td>1.72</td>
<td>3.36</td>
</tr>
<tr>
<td>2.29</td>
<td>3.76</td>
</tr>
<tr>
<td>3.43</td>
<td>4.33</td>
</tr>
<tr>
<td>4.58</td>
<td>4.73</td>
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<td>0.77</td>
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<td>0.00</td>
</tr>
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</table>
Table 3-2 - LOCATION OF PRESSURE ORIFICES
ON THE AIRFOIL MODEL SURFACES

<table>
<thead>
<tr>
<th>Tube Number</th>
<th>Surface</th>
<th>Butt Line</th>
<th>X/C</th>
<th>Number</th>
<th>Surface</th>
<th>Butt Line</th>
<th>X/C</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Upper</td>
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<td>0.00</td>
<td>22</td>
<td>Upper</td>
<td>-3.0</td>
<td>0.05</td>
</tr>
<tr>
<td>2</td>
<td>Upper</td>
<td>0.0</td>
<td>0.01</td>
<td>23</td>
<td>Upper</td>
<td>-3.0</td>
<td>0.40</td>
</tr>
<tr>
<td>3</td>
<td>Upper</td>
<td>0.0</td>
<td>0.05</td>
<td>24</td>
<td>Upper</td>
<td>-3.0</td>
<td>0.69</td>
</tr>
<tr>
<td>4</td>
<td>Upper</td>
<td>0.0</td>
<td>0.15</td>
<td>25</td>
<td>Upper</td>
<td>-3.0</td>
<td>0.90</td>
</tr>
<tr>
<td>5</td>
<td>Upper</td>
<td>0.0</td>
<td>0.40</td>
<td>26</td>
<td>Upper</td>
<td>-3.0</td>
<td>0.96</td>
</tr>
<tr>
<td>6</td>
<td>Upper</td>
<td>0.0</td>
<td>0.60</td>
<td>27</td>
<td>Lower</td>
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<td>0.02</td>
</tr>
<tr>
<td>7</td>
<td>Upper</td>
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<td>0.65</td>
<td>28</td>
<td>Lower</td>
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<td>0.04</td>
</tr>
<tr>
<td>8</td>
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<td>0.0</td>
<td>0.69</td>
<td>29</td>
<td>Lower</td>
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<td>0.10</td>
</tr>
<tr>
<td>9</td>
<td>Upper</td>
<td>0.0</td>
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<td>30</td>
<td>Lower</td>
<td>0.0</td>
<td>0.35</td>
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<td>Upper</td>
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<td>0.78</td>
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<td>11</td>
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<td>12</td>
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<td>13</td>
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<td>0.90</td>
<td>34</td>
<td>Lower</td>
<td>0.0</td>
<td>0.90</td>
</tr>
<tr>
<td>14</td>
<td>Upper</td>
<td>0.0</td>
<td>0.93</td>
<td>35</td>
<td>Lower</td>
<td>0.0</td>
<td>0.95</td>
</tr>
<tr>
<td>15</td>
<td>Upper</td>
<td>0.0</td>
<td>0.96</td>
<td>36</td>
<td>Lower</td>
<td>0.0</td>
<td>0.985</td>
</tr>
<tr>
<td>16</td>
<td>Upper</td>
<td>0.0</td>
<td>0.985</td>
<td>37</td>
<td>Lower</td>
<td>+3.0</td>
<td>0.35</td>
</tr>
<tr>
<td>17</td>
<td>Upper</td>
<td>+3.0</td>
<td>0.05</td>
<td>38</td>
<td>Lower</td>
<td>+3.0</td>
<td>0.985</td>
</tr>
<tr>
<td>18</td>
<td>Upper</td>
<td>+3.0</td>
<td>0.40</td>
<td>39</td>
<td>Lower</td>
<td>-3.0</td>
<td>0.35</td>
</tr>
<tr>
<td>19</td>
<td>Upper</td>
<td>+3.0</td>
<td>0.69</td>
<td>40</td>
<td>Lower</td>
<td>-3.0</td>
<td>0.90</td>
</tr>
<tr>
<td>20</td>
<td>Upper</td>
<td>+3.0</td>
<td>0.93</td>
<td>41</td>
<td>Lower</td>
<td>-3.0</td>
<td>0.985</td>
</tr>
<tr>
<td>21</td>
<td>Upper</td>
<td>+3.0</td>
<td>0.985</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
is located in the Aerospace Sciences Laboratory of the Lockheed-Georgia Company. The cross-section of the test section is rectangular having an area of 0.836 square meters with a height to width ratio of 0.7; this results in a test section of approximately 76.2 x 109.2 centimeters with a length of 122 centimeters. This wind tunnel is a closed circuit, single return, low speed wind tunnel and is powered by a 400 horsepower synchronous speed induction motor driving a 183 centimeters, 10-bladed, single stage axial flow fan. The fan speed can be varied over the range from 0 to 1150 revolutions per minute by means of an eddy current clutch. A standard friction brake rated at 103.7 meters kilogram torque will bring the fan to rest from full speed in less than 15 seconds.

The velocity range of the tunnel with an empty test section is 0 to 91.5 meters per second resulting in a maximum dynamic pressure of 508.3 kilograms per square meter. The dynamic pressure variations in the test section can be maintained within approximately 0.1 percent during a boundary layer survey which typically takes about ten minutes in this test facility. With the present screens, the turbulence factor is less than 1.3 based on turbulence sphere measurements. At a dynamic pressure of 293 kilograms per square meter, the nominal Reynolds number is \( \frac{439 \times 10^6}{\text{per meter of chord length}} \). A general layout of the test facility is shown in Figure III-5.

Figure III-6 shows the variation of static pressure along the test section centerline at various free stream dynamic pressures. This figure shows that the static pressure at the location of the quarter chord of the model is within about 3 percent of the static pressure at any other location on the model centerline. The calibration done at the location of the model quarter chord was used in reducing data from these tests. This calibration is shown in Figure III-7.

The test section is equipped with blowing boundary layer control slits on the ceiling floor. The auxiliary air, for boundary layer control on the test section side walls, is provided from the storage tanks which are capable of delivering air at a maximum pressure of 22.73 kilograms per square centimeter. A regulator reduces this pressure to 11.23 kilograms per square centimeter, a few meters from the test section. This air is further throttled to a desired pressure in the plenums for boundary layer control slits.

The boundary layer control slits, which are located on the ceiling and the floor of the test section, are 77.2 centimeters wide and .0635 centimeters high. These slits are located approximately one chord length upstream of the model leading edge. The heights of the blowing slits can be varied from .0254 to .254 centimeters. Relatively high velocity air is introduced through the slits into the tunnel floor and ceiling boundary layers such that boundary layer separation is prevented on these side walls when the given model is in the tunnel at a given angle of attack with respect to the free stream. When the angles of attack for a given two-dimensional model in the tunnel is increased from low values to high values corresponding to \( C_{L_{\text{MAX}}} \) condition, the boundary layer separation on the tunnel side walls occur upstream of the model leading edge due to the presence of adverse pressure gradient in the flow direction. If the above flow separation is present, then the reliable two-dimensional data cannot be obtained, especially near the \( C_{L_{\text{MAX}}} \) conditions. The method used to determine the proper amount of blowing and the desired pressure ratio required to eliminate this boundary layer separation on wind tunnel side walls is discussed in
RESEARCH LOW SPEED WIND TUNNEL

FIGURE III-5 - TEST FACILITY GENERAL LAYOUT
FIGURE III-6 - TEST SECTION STATIC PRESSURE VARIATION

- Local static pressure
- Ambient pressure
- Local dynamic pressure

Model T.E. Location

<table>
<thead>
<tr>
<th>q</th>
<th>P - Po</th>
</tr>
</thead>
<tbody>
<tr>
<td>293 Kgm/sq. meter</td>
<td>( \frac{P - P_o}{q} )</td>
</tr>
<tr>
<td>195 Kgm/sq. meter</td>
<td>0.12</td>
</tr>
<tr>
<td>975 Kgm/sq. meter</td>
<td>0.12</td>
</tr>
</tbody>
</table>

\( c_{REF} = 2.64 \) Centimeters

DISTANCE FROM 1/4 CHORD/\( c_{REF} \)
1/4 CHORD LOCATION

FIGURE III-7 - TEST SECTION SPEED CALIBRATION
Appendix A. Figure III-8 shows the details of the boundary layer control system used for the present study contract.

III-3 Special Instrumentation - Pressure Probe, Hot Wire Anemometer and Probe Drive Mechanism

Measurements of flow in the wake as well as on the upper surface of the test airfoil in the vicinity of the trailing edge were performed by the use of pressure probe and hot wire anemometer probe. In the case of sharp trailing edge airfoil, flow measurements in the airfoil wake were performed by use of a special type of pressure probe, whereas in the case of test airfoil with 1 percent thick trailing edge, hot wire anemometer probe was used for wake flow measurements. In the case of blunt trailing edge, the velocity profiles have reverse flow or negative velocity associated with them up to some distance downstream of the trailing edge. Hence, in this case, pressure probe alone cannot be used for determination of flow quantities in this region and hence additional instrumentation, such as hot wire probe is necessary for the determination of flow associated with reverse flow velocity profiles. The following paragraphs give the brief description of these two types of probes used during the experimental program of the present studies. The details of the additional instrumentation, used for acquisition and reduction of experimental measurements is given in Appendix B.

III.3.1 Pressure Probe: Measurements of static and total pressures in the boundary layer, for the purpose of determining boundary layer velocity profiles in the wake of test airfoil as well as on its upper surface, were made using the specially designed three element probe shown in Figure III-9. For the purpose of developing accurate mathematical model for the calculation of wake flow and profile drag of airfoils, it was desired to know the true variations of static pressure distribution across the boundary layer of the airfoil wake at various chordwise locations. Hence the selection of the probe tip design for the measurements of total and static pressures in the viscous flow in the wake was based on the following considerations.

It is known that it is relatively easy to measure an accurate value of total pressure in the flow with little effect arising from incidental variation in flow conditions. On the other hand, the selection of hole positions on the probe for the measurements of static pressure in the flow is much more difficult. This is so because it is observed that the measured value of the static pressure in the flow is very sensitive to any parameters, e.g. pitch angle of the probe, local flow direction in the flow, conditions of the boundary layer on the probe, proximity of static pressure holes on the probe to solid boundaries and several other factors. The above difficulty can, however, be overcome if it is possible to derive the value of velocity in the boundary layer from measured pressure differences arising between holes which sense pressures greater than the local static pressure but which show negligible sensitivity to the above-mentioned parameters. A simple relationship for dynamic pressure then still holds good and can be expressed as,
FIGURE III-8 - BOUNDARY LAYER CONTROL SYSTEM
STATIC PRESSURE ELEMENTS WITH CENTER TOTAL PRESSURE ORIFICE

TOTAL PRESSURE ELEMENT

FIGURE III-9 - TOTAL-STATIC PRESSURE PROBE COMBINATION
\[
\frac{1}{2} \rho u^2 = K (P_1 - P_2) \tag{111-1}
\]

where  
\( \rho \) = density  
\( u \) = local velocity  
\( P_1 \) and \( P_2 \) = pressure sensed at different hole positions, and  
\( K \) = calibration factor and may depend on wind speed.

Probe tip design which has been fabricated in order to remove the above-mentioned sensitivities, and hence to be able to measure local velocity and static pressure to desired accuracy are shown in Figures III-9 and III-10. The upper and lower right-hand elements are designed to measure both static and total pressures. The lower left-hand element is designed to measure total pressure only. The total pressure tubes on the lower element thus provide a check or comparison. The upper element is used when the thickness of boundary layer exceed 7.62 centimeters.

The left-hand, or total pressure, element is constructed of 0.0508 centimeter diameter stainless steel tubing which has been flattened at the end as shown in Figure III-10. Measurement of total pressure within .01015 centimeters of the model surface are possible with this arrangement. The two side orifices were tied together and used to measure static pressure in the boundary layer. They were formed by beveling the side tubes 30 degrees as shown in Figure III-10.

The beveled tubes on the upper and lower elements measure the pressure which is proportional to the difference between total and static pressure and this measured pressure is related to local dynamic pressure by a calibration factor. Thus, a calibration of the probe reading versus true static and total pressure is necessary. Calibration of the probe was accomplished in the wind tunnel using the pitot tube shown in Figure III-11 as a reference. The calibration curves for this probe are shown in Figure III-12. The above calibration was performed in the absence of the solid boundary and therefore the correction due to the presence of wall proximity has to be used when velocity profile measurements near the solid surface are desired. It has been found by MacMillan (Reference 4) that, for a pitot tube of ratio \( d/D = 0.6 \), the measured value of total pressure is less than the true value when the probe is nearer than \( 2D \) to a solid boundary; here \( d \) is equal to internal diameter of the tubing and \( D \) is equal to outside diameter of the tubing for the total pressure tube. This wall proximity correction curve, which is obtained from Reference 4, is shown in Figure III-13. Thus, total pressure, static pressure, and dynamic pressure at a desired point in the boundary layer is determined from measurements by probe, which is shown in Figure III-9, as given by the following expressions:

\[
P_{TC} = P_T + P_{TCW} \\
q = \frac{1}{2} \rho u^2 = K_{CL} (P_{TC} - P_{12}) \\
P_{Sc} = P_{TC} - K_{CL} (P_{TC} - P_{12}) \\
u = \sqrt{\frac{2q}{(P_{Sc}/RT)}}
\]
FIGURE III-10 - DETAILS OF BOUNDARY LAYER PROBES
Figure III-11 - CALIBRATION PITOT TUBE
\[ q_{\text{REF}} = 4.88 \text{ KILOGRAM/SQ. METER} \]

**Figure III-12 - Boundary Layer Probe Calibration**

[Graph showing static and total pressure vs. Pitot tube pressure relative to \( q_{\text{REF}} \)]
$Y = \text{Percent } \frac{1}{2} Q u^2 \text{ to be added to pitot reading}$

**FIGURE III-13 - WALL PROXIMITY CORRECTION FOR TOTAL PRESSURE TUBE MEASUREMENTS**
where \( P_T \) = corrected value of total pressure
\( P_{TC} \) = total pressure measured by total pressure tube \( P \) and corrected by the use of Figure 11-12
\( P_{TWC} \) = wall proximity correction from Figure 11-13
\( P_{Sc} \) = corrected value of static pressure
\( K_{CL} \) = calibration factor for obtaining static pressure from measured pressure by bevelled tube \( P_{12} \) by the use of curves in Figure 11-12
\( u \) = local velocity in boundary layer
\( R \) = universal gas constant
and \( T \) = absolute temperature in boundary layer.

11.3.2 Hot Wire Anemometer Probe: The hot wire anemometer is a very desirable instrument for the analysis of the micro structure of the velocity of a streaming gas or liquid. Measurements of the micro structure of a stream require a very small sensitive element having a short response time, sufficient sensitivity and little disturbing effect on the original stream. The sensitive element is a thin electrically heated wire suspended between two needle points or a thin electrically heated metal film fused to a glass support. Figure 11-14 shows the sketch of hot wire anemometer probe; also shown in this figure is the enlarged view of the single sensor and sensor support used during the present investigation.

The use of a hot sensing element for measurements of particle velocity in fluid flows relies on laws governing convective heat transfer. These laws are generally too complicated to permit a theoretical calculation of the relation between the particle velocity and the heat flux from a probe, and the relation must therefore be found experimentally using laws of similarity. The expression for the heat flux for the two-dimensional heat transfer in an incompressible potential flow is given by

\[
Q = K \left\{ 1 + \sqrt{\frac{2\pi \rho C_p U}{K}} \right\} (T - T_0) \tag{111-3}
\]

where \( Q \) = heat flux
\( K \) = thermal conductivity
\( \ell \) = length of wire
\( \rho \) = density of fluid
\( C_p \) = constant pressure specific heat of fluid
\( d \) = diameter of wire
\( U \) = flow velocity
\( T \) = film temperature
\( T_0 \) = fluid temperature.

For thermal-equilibrium conditions, the rate of heat loss from the hot wire must be equal to the heating power generated in the wire by the electric current. i.e. it must equal to \( I^2 R/J \). Thus, from the point of view of hot wire anemometry,
FIGURE III-14 - HOT WIRE ANEMOMETER PROBE WITH SINGLE SENSOR
the relation between fluid particle velocity and electrically generated heating power takes the following form:

\[
\frac{R}{R - R_0} \quad 1^2 = A + B \sqrt{U}
\]  \hspace{1cm} (111-4)

where \(A\) and \(B\) are constant for a specific probe wire operating in a specific fluid,

\(u = \) local velocity  \\
\(I = \) heating current  \\
\(R = \) operating resistance of hot wire  \\
\(R_0 = \) resistance of hot wire at fluid temperature.

However, empirical formula for the heat loss from a hot wire is generally written as,

\[
\frac{R}{R - R_0} \quad 1^2 = A + B (u)^n
\]  \hspace{1cm} (111-5)

and the values of \(A\), \(B\) and \(n\) are determined from calibration. The output of the anemometer is a bridge voltage \(V\), and the squared voltages \(V^2\) and \(V_0^2\) (\(V_0 = \) bridge voltage when the flow velocity is zero) are linearly related to the heat loss of the wire at the velocity in question and at zero velocity respectively. At very low velocities, the heat loss due to forced convection is a minor part of the total heat loss, resulting in a typical calibration curve shown in Figure 111-15(a). In cases where measurements of high degrees of turbulence are concerned, such as in wake flow behind airfoil, or where large series of measurements are to be performed over a wide velocity range at a great number of points, such as in the case of boundary layer measurements over the surface of an airfoil, it is necessary to linearize the anemometer output voltage in order to avoid nonlinear distortion and ensure rational operation. Figure 111-15(b) shows the plot of anemometer output voltage versus flow velocity made in conjunction with hot wire anemometer.

There are two modes of operation for the hot-wire anemometer, namely, (i) constant-current operation and (ii) constant-temperature operations. The constant current operation has the advantage of simpler electronic circuitry, however, constant temperature operation is more suited for measurements of fluctuations of high frequencies such as turbulent flows and measurements of a boundary layer where velocity varies from essentially zero near the wall to large value at the edge of the boundary layer in a small boundary layer thickness. Another advantage of constant temperature operation is the possibility of obtaining greater flow sensitivity using high overheating with no risk of probe burn out owing to sudden velocity decrease. For these reasons the constant temperature operation for hot wire anemometer was used during the present experimental measurements in the case of blunt trailing edge airfoil test. The constant temperature operation is briefly described in the following paragraph.

Figure 111-16 shows the schematic diagram of the principle of constant temperature operation. The idea behind the constant temperature system is to minimize the effect of thermal inertia of the probe by keeping the sensitive
U_{Ref} = 30.5 \text{ Centimeter/Sec.}

Without Linearizer

\[ V = \left[ A + B \frac{u}{U_{Ref}} \right]^{1/2} \]

In Presence of Linearizer

\[ V = k u \]

**FIGURE III-15** - RELATION BETWEEN ANEMOMETER VOLTAGE AND FLOW VELOCITY WITH AND WITHOUT THE USE OF LINEARIZER
FIGURE III-16 - PRINCIPLE OF CONSTANT TEMPERATURE ANEMOMETER
element at a constant temperature and using the heating current as the measure of heat transfer and hence velocity. The principle requires a sophisticated and well-designed electronic system, and it can be explained with the aid of Figure III-16. Under conditions of bridge balance a voltage is present across the vertical bridge diagonal. This voltage is supplied by the servo amplifier. A slight change in the convective cooling of the sensor will cause a small voltage to appear across the horizontal diagonal. The latter voltage, after having undergone considerable amplification, is fed back to the vertical bridge diagonal, its polarity being selected so that it will automatically balance the bridge. In this way the temperature variations of the hot wire are kept extremely small.

III.3.3 Probe Drive Mechanism: Boundary layer surveys, both on the upper surface of the airfoil and as in the wake behind airfoil trailing edge, were obtained by using an automatic probe drive unit to position the probe at pre-selected heights above the model surface. This unit, which is shown in Figure III-17, was mounted on the outside wall of the test section with the probe extending through the wall into the test section. Figure III-18 shows the typical model installation in the wind tunnel facility and the boundary layer probe arrangement. This boundary layer probe can be located at any chordwise location on the airfoil as well as in the wake aft of the airfoil trailing edge. This probe is driven in the direction perpendicular to the local airfoil contour or normal to the airfoil chord line extended in the airfoil wake. The probe drive units consist of a 28-volt gear motor, lead screw, drive nut, and position potentiometer. A maximum of 50 pre-selected probe positions may be input to the data acquisition program. During the run the program will signal the gear motor to drive the probe assembly to each position in sequence and pause while the data are read by a scanivalve and transducer located outside the test section. Minimum increment of probe travel is .0318 centimeters. Maximum travel is 8.9 centimeters. Limit switches are provided to protect the mechanism and prevent the probe from crashing into the model.

A multi-post scanivalve and pressure transducer were used to read the probe pressures. The transducer is rated at 0.175 kilogram per square centimeter differential.

III.4 Data Acquisition, Reduction and Analysis Technique

Figure III-19 shows the schematic diagram of the system used for the acquisition and reduction of the raw test data. The heart of the system is a Lockheed Electronics MAC 16 computer. The raw data was made available, in an abbreviated form, on teletype for outline monitoring of the test, and in its entirety on paper tape using a high speed punch. Final data reduction was accomplished on a Univac 1106 Central Computing System with remote access terminals. General description of Data Acquisition Unit and MAC 16 computer is given in Appendix C. In this section, however, main equations used in the reduction of data are given and the technique and the computer program for the analysis of data in the airfoil wake is described in brief.
Figure III-17 - PROBE DRIVE UNIT
FIGURE III-18 - MODEL INSTALLATION - FRONT VIEW

UPPER SURFACE SUCTION PIPE

BOUNDARY LAYER CONTROL SLOT

BOUNDARY LAYER PROBE
FIGURE III-19 - SCHEMATIC REPRESENTATION OF TYPES OF MODELS FOR TEST AND COORDINATE SYSTEM FOR MEASUREMENTS
III.4.1 Data Reduction Equations: Tunnel conditions acquired at each data point consisted of the dynamic pressure \( q_m \), the total pressure \( H_m \), and the total temperature \( T_m \). In addition the barometric pressure \( P_a \) is obtained from a thumb-wheel switch input. These conditions are corrected to free stream condition using standard procedure.

An initial estimate of the free stream Mach number,

\[
M_{est}^2 = \frac{q_m}{0.7 P_a (1 + 0.000171 q_m)}
\]

(III-6)

from which the compressible dynamic pressure is,

\[
q = q_m (1 + K (2 - M_{est}^2))
\]

(III-7)

where \( K \) is the solid blockage factor calculated as,

\[
K = \frac{K_1 \tau V_w}{(C (1 - M_{est}^2))^{3/2}} + \left(1 + 0.4 \frac{M_{est}^2}{1 - M_{est}^2}\right) \left(0.5 \frac{C_{Dm} \bar{C}}{W_t}\right)
\]

(III-8)

where \( K_1 \) = wing blockage shape factor

\( \tau \) = blockage correction factor relating model span to tunnel width

\( V_w \) = model volume (cubic feet)

\( C_1 \) = tunnel cross-sectional area (square feet)

\( C_{Dm} \) = model drag coefficient

\( \bar{C} \) = model chord

and \( W_t \) = tunnel width (ft)

The free stream total pressure is

\[
P_{T_\infty} = H_m + K_{H_1} q_m + K_{H_2} q_m^2 + P_a
\]

(III-9)

where \( K_{H_1} \) and \( K_{H_2} \) are airstream calibration constants for the test section.

The free stream static pressure, mach number, static temperature and density can now be calculated as,

\[
P_{S_\infty} = P_{T_\infty} - q_\infty
\]

\[
M_\infty = \left\{ 5 \left(\frac{P_{T_\infty}}{P_S} \right)^{286} \right\}^{1/2}
\]
The finally reduced data assumes incompressible flow conditions and are therefore based on

\[ T_\infty = \frac{T_m + 459.6}{1 + 0.2 M_\infty^2} \]

\[ \rho_\infty = 0.005826 \left( \frac{P_T}{T_\infty} \right) \]  \hspace{1cm} (111-10)

Denoting the static pressure measured during the surveys as \( P_s \), then,

\[ \frac{q_m}{q_o} = 2.4 \]  \hspace{1cm} (111-11)

\[ \frac{m}{40} + \frac{M_\infty^6}{1000} \]

and

\[ \rho_o = \rho_\infty (1 - 0.2 M_\infty^2)^{2.5} \]

from which the local Mach number and density are obtained...

\[ M_e = \left[ 5 \left\{ \frac{1 + 0.2 M_\infty^2}{(1 + 0.7 M_\infty^2 \frac{C_p}{M_\infty^2})^{0.286}} - 1 \right\} \right]^{1/2} \]  \hspace{1cm} (111-13)

and

\[ \rho_e = \rho_o \left( \frac{1 + 0.2 M_\infty^2}{1 + 0.2 M_e^2} \right)^{2.5} \]

III.4.2 Data Analysis Technique: A computer program was formulated for the data analysis of the experimental measurements obtained in the viscous flow in the wake. Experimentally measured variations across the airfoil wake, values of total pressure, static pressure, ambient temperature, free stream dynamic head and barometric pressures at two closely spaced \( X \) locations constitute the input to the computer program for the data analysis. This program then calculates and prints out the following information:

(1) nondimensional velocity profiles at two stations,

(2) shear stress distribution across the viscous layer between two stations,
(3) mixing length distribution, eddy viscosity ratio distribution, and slope of the velocity profiles at various y locations for the station between input stations \(X_1\) and \(X_2\),

(4) turbulent dissipation or shear work integral, displacement thickness, momentum thickness and energy thickness at both stations,

(5) similarity parameters for velocity profile and values of similarity functions at both stations, and

(6) various integrals at each station, such as

\[
\int_0^{y_1} \frac{u}{U_e} \, dy, \quad \int_0^{y_1} \left(\frac{u}{U_e}\right)^2 \, dy, \quad \int_0^{y_1} \frac{u}{U_e} \, dy,
\]

and

\[
u(y_1) \cdot \int_0^{y_1} \left(\frac{u}{U_e}\right)^2 \, dy,
\]

where \(y_1\) is any distance above the lower edge of wake in the viscous region and \(u(y_1)\) is the velocity at distance \(y\) above the lower edge of wake.

In order to calculate the shear distribution, eddy viscosity ratio and variation of mixing length across the airfoil wake the use of the following basic equations was made:

\[
\rho V = - \int_0^y \frac{\partial}{\partial x} \left(\rho u\right) \, dy + \rho_0 V_0 \quad (111-14)
\]

\[
\rho u \frac{\partial u}{\partial x} + \rho v \frac{\partial u}{\partial y} = - \frac{\partial p}{\partial x} + \frac{\partial \tau}{\partial y} \quad (111-15)
\]

The Euler equation at the upper and lower edges of the wake is given by

\[
\frac{dP_{eL}}{dx} = - \rho_{eL} U_{eL} \frac{dU_{eL}}{dx} \quad ; \quad \frac{dP_{eu}}{dx} = - \rho_{eu} U_{eu} \frac{dU_{eu}}{dx} \quad (111-16)
\]

By integrating equation (111-15) from \(y = 0\) (lower edge of wake) to \(y = y_1\), making use of equations (111-14) and (111-16), and using Leibnitz's rule and integration by parts, the following equation can be derived after some algebraic simplification:
\[
\frac{\tau(y_1)}{\rho_{em} U_{em}^2} - \frac{\tau_0}{\rho_{em} U_{em}^2} + \left( \frac{\rho_o}{\rho_{em}} \right) \left( \frac{V_o}{U_{em}} \right) \left\{ \frac{U_{el}}{U_{em}} - \frac{U(y_1)}{U_{em}} \right\}
\]

\[
= + \frac{d}{dx} \left[ \int_0^{y_1} \left( \frac{\rho}{\rho_{em}} \right) \left( \frac{u}{U_{em}} \right) dy \right] - \left( \frac{\rho(y_1)}{U_{em}} \right) \frac{\partial}{\partial x} \left[ \int_0^{y_1} \frac{\rho}{\rho_{em}} \frac{u}{U_{em}} dy \right]
\]

\[
+ \left( \frac{0.5}{U_{em}} \right) \left( \frac{dB_{em}}{dx} \right) \left[ \int_0^{y_1} \left( \frac{\rho}{\rho_{em}} \right) \left( \frac{u}{U_{em}} \right)^2 \left( \frac{U(y_1)}{U_{em}} \right) \left( \frac{\rho}{\rho_{em}} \right) \left( \frac{u}{U_{em}} \right) \left( \frac{u}{U_{em}} \right) \right] dy
\]

\[
+ \left( \frac{0.5}{U_{em}} \right) \left( \frac{dB_{em}}{dx} \right) \left[ \int_0^{y_1} \left( \frac{\rho}{\rho_{em}} \right) \left( \frac{u}{U_{em}} \right)^2 \left( \frac{U(y_1)}{U_{em}} \right) \left( \frac{\rho}{\rho_{em}} \right) \left( \frac{u}{U_{em}} \right) \right] dy
\]

\[
+ \left( \frac{0.5}{U_{em}} \right) \left( \frac{dB_{em}}{dx} \right) \left[ \int_0^{y_1} \left( \frac{\rho}{\rho_{em}} \right) \left( \frac{u}{U_{em}} \right)^2 \left( \frac{U_{el}}{U_{em}} \right) \left( \frac{\rho e_{el}}{\rho_{em}} \right) \right] dy
\]

\[
+ \left( \frac{0.5}{U_{em}} \right) \left( \frac{1}{\rho_{em}} \right) \left( \frac{dB_{em}}{dx} \right) \left[ \int_0^{y_1} \left( \frac{\partial P_{em}}{\partial x} - \frac{dP_{em}}{dx} \right) dy \right]
\]

\[
+ \left( \frac{0.5}{U_{em}} \right) \left( \frac{1}{\rho_{em}} \right) \left( \frac{dB_{em}}{dx} \right) \left[ \int_0^{y_1} \left( \frac{\partial P_{em}}{\partial x} - \frac{dP_{em}}{dx} \right) dy \right]
\]

(111-17)

In the above equation the symbols have the following meaning:

- \( y_1 \) = distance above the lower edge of wake
- \( U_{el} \) = average velocity at the lower edge of wake for stations \( X_1 \) and \( X_2 \)
- \( U_{eu} \) = average velocity at the upper edge of wake for stations \( X \) and \( X \)
- \( U_{em} \) = \( \frac{1}{2} \left( U_{el} + U_{eu} \right) \)
- \( y_o \) = lower edge of wake
\( V_0 \) = \( y \) component velocity at lower edge of wake

\( P_{e_u} \) = average value of pressure at the upper edge of wake

\( P_{e_L} \) = average value of pressure at the lower edge of wake

\( \rho_{e_u} \) = average value of density on the upper edge of wake between stations \( X_1 \) and \( X_2 \)

\( \rho_{e_L} \) = average value of density on the lower edge of wake between stations \( X_1 \) and \( X_2 \)

\( \rho_{e_m} = \frac{1}{2}(\rho_{e_u} + \rho_{e_L}) \)

\( \tau(y_1) \) = value of shear stress at \( y = y_1 \)

\( \tau(y_0) \) = value of shear stress at lower edge of wake

\( \tau = 0 \)

The right-hand side of equation (111-17) was programmed to compute shear stress \( \tau(y_1) \) at various distances, \( y_1 \), above the lower edge of the wake. Values of shear stresses at the lower and upper edges of the wake were assumed to be zero. The same equation and the same numerical procedure was used to compute shear distribution, eddy viscosity ratio and turbulent dissipation in the various regions of the wake flow behind the airfoil trailing edge.

### 11.4.3 Determination of Profile Drag from Experimental Measurements in the Wake

The expression due to Betz, Reference 11, for the computation of profile drag of airfoil from the measurements of total pressures and static pressures across the wake behind the airfoil is given by:

\[
C_D = \left\{ \left( \frac{P_{T_{\infty}} - P_T(y)}{q} \right) \right\} d \left( \frac{y}{c} \right) + \left\{ \left( \frac{P_{T_{\infty}} - P_T(y)}{q_{\infty}} \right)^{\frac{1}{2}} - \left( \frac{P_T(y) - P_{S(y)}}{q_{\infty}} \right)^{\frac{1}{2}} \right\} \left( \frac{P_T - P_{S(y)}}{q_{\infty}} \right)^{\frac{1}{2}} + \left( \frac{P_T(y) - P_{S(y)}}{q_{\infty}} \right)^{\frac{1}{2}} - 2 \right\} d \left( \frac{y}{c} \right) \tag{111-18}
\]

where

\( C_D \) = profile drag coefficient of an airfoil

\( P_{T_{\infty}} = P_Te \) = total pressure of free stream

\( P_{S(y)} \) = static pressure across the wake
\[ q_\infty = \text{free stream dynamic head} = \frac{1}{2} \rho U_\infty^2 \]
\[ P_\infty = \text{free stream static pressure} \]
\[ C = \text{airfoil chord} \]

If static pressure \( P_s(y) \) in the above equation (III-18) is assumed to be constant and equal to the free stream static pressure, the equation (III-18) can be simplified to,

\[
C_D = \int \left\{ -2 \left( \frac{P_T(y) - P_\infty}{q_\infty} \right) + 2 \left( \frac{P_T(y) - P_\infty}{q_\infty} \right)^{\frac{1}{2}} \right\} d \left( y/c \right)
\]

or

\[
C_D = 2 \int \left( \frac{u}{U_\infty} \right)^2 + \left( \frac{u}{U_\infty} \right) d \left( y/c \right) = 2 \int \frac{u}{U_\infty} \left( 1 - \frac{u}{U_\infty} \right) d \left( y/c \right)
\]

or

\[
C_D = 2 \frac{\theta_\infty}{c} \tag{III-19}
\]

where \( \theta_\infty \) = momentum thickness in the wake far downstream of airfoil trailing edge where static pressure has stabilized to the free stream value.

III.5 Types of Tests

Experimental data were obtained for model under two conditions, namely (i) sharp trailing edge airfoil, and (ii) blunt trailing edge airfoil. In the case of blunt trailing edge airfoil, the trailing edge thickness was kept at 1 percent of the airfoil chord. Airfoil chord \( c \) is equal to 29.2 centimeters and the value of the maximum thickness of the airfoil tested is 15 percent. The chord of the blunt trailing edge airfoil is approximately equal to 0.975\( c \) where \( c \) is the chord of the airfoil with the sharp trailing edge. This is shown schematically in Figure III-19 which also shows the coordinate system under which measurements are obtained during the tests.

All the measurements taken during the tests were obtained at free stream Mach number of approximately 0.2 and free stream dynamic head of 60 psf. These conditions would correspond to the free stream Reynolds number during the tests of approximately 1.3 to 1.4 million based on the chord of the airfoil. Measurements were obtained at angles of attack between 0° and angle of attack corresponding to the occurrence of incipient separation on the upper surface of the airfoil in the neighborhood of the trailing edge; the latter condition corresponds to \( \alpha = 10.8^\circ \) for the test airfoil. Measurements taken during the tests can be classified by two categories namely (i) measurements of pressure distribution on the surface of the airfoil and (ii) viscous flow measurements. Measurements of the viscous flow were obtained both on the upper surface of the
airfoil in the neighborhood of the trailing edge as well as in the airfoil wake at several chordwise locations.

In the case of test airfoil with the sharp trailing edge the viscous flow measurements were obtained by the use of total-static pressure probe which is shown in Figure III-9. For the test airfoil with the blunt trailing edge, the viscous flow measurements were obtained by use of hot wire anemometer probe which is shown in Figure III-14. Figures III-20, III-21 and III-22 show the plots of measured pressure distributions and loci of minimum velocity for the airfoil with the sharp trailing edge. Boundary layer surveys in the airfoil wake have been obtained at $x'/c$ locations shown in Figures III-20, III-21 and III-22 by triangular symbols. These measurements are tabulated in Table 3-3 for sharp trailing edge airfoil. As seen in this table these measurements at a particular chordwise location $x'/c$ in the wake consist of values of (i) total pressure $P_t$, (ii) static pressure $P_s$, (iii) free stream dynamic head $q_m$, and (iv) ratio of $u/U_w$ at several $y$ locations in the wake flow boundary layer.

Figure III-23, III-24 and III-25 show the plot of measured pressure distributions on the airfoil surface and the loci of minimum velocity in the wake of blunt trailing edge test airfoil. Boundary layer surveys are obtained, by the use of hot wire anemometer probe, at the chordwise locations $s'/c$ indicated by the triangular symbols on measured loci in the above figures. The boundary layer surveys consist of measurements of $u/U_w$ or $u/U_e$ and $q_m$ for several $y$ values and for a given chordwise location $x'/c$. These measurements are tabulated in Table 3-4.

IV. RESULTS AND DISCUSSION

In the previous section the description of experimental facility, type and number of experimental measurements and the computer program for the analysis of the fundamental wake flow parameters was given. From the output of this computer program various physical parameters for the flow in the wake of single component airfoils are studied. The relationships among various physical parameters, which appeared in theoretical equations of Section II, are derived and described in this section. Establishment of these relationships between various physical parameters is of vital importance for the prediction of drag of arbitrary configuration single or multi-component airfoils by the generalized theoretical method such as described in this report.

IV.1 Presentation of Typical Measured Experimental Data

Figures IV-1 through IV-5 show the measurements of velocity profile on the upper surface of the test airfoil with sharp trailing edge. These measurements were performed by use of total-static pressure probe combination shown schematically in Figure III-9. This type of boundary layer measurements on the surface of the airfoil were obtained for the purpose of determining (i) the approximate location of the point of incipient separation on the airfoil
Table 3-3 - MEASUREMENTS IN THE WAKE OF SHARP T.E. TEST AIRFOIL BY THE USE OF TOTAL STATIC PRESSURE PROBE COMBINATION

<table>
<thead>
<tr>
<th>Station 1</th>
<th>q_n = 1.80 kilogram/square meter</th>
</tr>
</thead>
<tbody>
<tr>
<td>z/A</td>
<td>x/A = 0.03 μm</td>
</tr>
<tr>
<td>Press Mean Total Pressure/ρg</td>
<td>2123.0</td>
</tr>
<tr>
<td>Press Mean Static Pressure/ρg</td>
<td>1234.5</td>
</tr>
<tr>
<td>Press Mean Total Temperature</td>
<td>2345.6</td>
</tr>
</tbody>
</table>

| z/A      | x/A = 0.03 μm                   |
| Press Mean Total Pressure/ρg  | 2123.0                           |
| Press Mean Static Pressure/ρg  | 1234.5                           |
| Press Mean Total Temperature  | 2345.6                           |

<table>
<thead>
<tr>
<th>Station 2</th>
<th>q_n = 1.80 kilogram/square meter</th>
</tr>
</thead>
<tbody>
<tr>
<td>z/A</td>
<td>x/A = 0.03 μm</td>
</tr>
<tr>
<td>Press Mean Total Pressure/ρg</td>
<td>2123.0</td>
</tr>
<tr>
<td>Press Mean Static Pressure/ρg</td>
<td>1234.5</td>
</tr>
<tr>
<td>Press Mean Total Temperature</td>
<td>2345.6</td>
</tr>
</tbody>
</table>

| z/A      | x/A = 0.03 μm                   |
| Press Mean Total Pressure/ρg  | 2123.0                           |
| Press Mean Static Pressure/ρg  | 1234.5                           |
| Press Mean Total Temperature  | 2345.6                           |

<table>
<thead>
<tr>
<th>Station 3</th>
<th>q_n = 1.80 kilogram/square meter</th>
</tr>
</thead>
<tbody>
<tr>
<td>z/A</td>
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</tr>
<tr>
<td>Press Mean Total Pressure/ρg</td>
<td>2123.0</td>
</tr>
<tr>
<td>Press Mean Static Pressure/ρg</td>
<td>1234.5</td>
</tr>
<tr>
<td>Press Mean Total Temperature</td>
<td>2345.6</td>
</tr>
</tbody>
</table>

| z/A      | x/A = 0.03 μm                   |
| Press Mean Total Pressure/ρg  | 2123.0                           |
| Press Mean Static Pressure/ρg  | 1234.5                           |
| Press Mean Total Temperature  | 2345.6                           |

<table>
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<th>Station 4</th>
<th>q_n = 1.80 kilogram/square meter</th>
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<td>1234.5</td>
</tr>
<tr>
<td>Press Mean Total Temperature</td>
<td>2345.6</td>
</tr>
</tbody>
</table>

| z/A      | x/A = 0.03 μm                   |
| Press Mean Total Pressure/ρg  | 2123.0                           |
| Press Mean Static Pressure/ρg  | 1234.5                           |
| Press Mean Total Temperature  | 2345.6                           |

71
### Table 3-3 (Continued)

<table>
<thead>
<tr>
<th>( x / h )</th>
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<th>( u_{\infty} )</th>
<th>( T_{\infty} )</th>
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### Station 3: \( \rho_{\infty} = 1.00 \) Kilogram/square meter

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<th>( x / h )</th>
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### Station 4: \( \rho_{\infty} = 1.00 \) Kilogram/square meter

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72
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<th>$\rho$ (g/cm$^3$)</th>
<th>$k$ (W/m$^\circ$C)</th>
<th>$\lambda$ (W/m$^\circ$C)</th>
<th>$\gamma$ (J/kg$^\circ$C)</th>
<th>$S_e$ (W/m$^2$K)</th>
<th>$S_w$ (W/m$^2$K)</th>
<th>$F_e$ (W/m$^2$K)</th>
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Table 3-3 (Continued)
### Table 3-3 (Continued)

#### Station 1: \( \rho = 1.8 \) Kilogram/meter$^3$

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#### Station 2: \( \rho = 1.8 \) Kilogram/meter$^3$

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Table 3-4 - MEASUREMENTS IN THE WAKE OF BLUNT T.E. TEST AIRFOIL
BY THE USE OF HOT-WIRE ANEMOMETER PROBE

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<th>Station 3 - Free Transition</th>
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### Table 3-4 (Continued)

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### Table 3-4 (Continued)

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#### Station 3 - Free Transition

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Airfoil Pressure Distribution

\[ \alpha = 0^\circ, \quad R_N \approx 1.3 \times 10^6 \]

\[ M_\infty = 0.201 \]

○ Upper Surface

○ Lower Surface

FIGURE III-20 - MEASURED PRESSURE DISTRIBUTION ON AIRFOIL SURFACE AND LOCUS OF MINIMUM VELOCITY AT \( \alpha = 0^\circ \) (FREE TRANSITION)
Airfoil Pressure Distribution

\[ \alpha = 8^\circ; \quad Re \approx 1.3 \times 10^6 \]
\[ M_\infty = 0.201 \]

\[ \bigcirc \quad \text{Upper Surface} \]
\[ \bigcirc \quad \text{Lower Surface} \]

FIGURE III-21 - MEASURED PRESSURE DISTRIBUTION ON AIRFOIL SURFACE AND LOCUS OF MINIMUM VELOCITY AT \( \alpha = 8.0^\circ \) (FREE TRANSITION)
Airfoil Pressure Distribution

$\alpha = 10.8^\circ; \quad R_n = 1.3 \times 10^6$

$M_\infty = 0.201$

Upper Surface

Lower Surface

FIGURE III-22 - MEASURED PRESSURE DISTRIBUTION ON AIRFOIL SURFACE AND LOCUS OF MINIMUM VELOCITY AT $\alpha = 10.8^\circ$ (FREE TRANSITION)
Blunt Trailing Edge (T.E. t/c = .01)

Airfoil Pressure Distribution

- Upper Surface
- Lower Surface

\[ \alpha = 0.0^\circ; \quad R_N = 1.3 \times 10^6 \]

\[ M_\infty = 0.201 \]

Airfoil Trailing Edge

\[ \Delta \] Measured Locus of Minimum Velocity

\[ \bullet \]

**FIGURE III-23** - MEASURED PRESSURE DISTRIBUTION ON AIRFOIL SURFACE AND LOCI OF MINIMUM VELOCITY AT $\alpha = 0^\circ$

FOR BLUNT TRAILING EDGE AIRFOIL (FREE TRANSITION)
Blunt Trailing Edge (T.E. \( t/c = .01 \))

Airfoil Pressure Distribution

\( \alpha = 4.0^\circ \); \( R_{\infty} \leq 1.3 \times 10^6 \)

\( M_{\infty} = 0.201 \)

Airfoil Pressure Distribution

Airfoil Trailing Edge

Measured Locus of Minimum Velocity in the Airfoil Wake

FIGURE III-24 - MEASURED PRESSURE DISTRIBUTION ON AIRFOIL SURFACE AND LOCUS OF MINIMUM VELOCITY IN THE WAKE FOR BLUNT TRAILING EDGE AIRFOIL AT \( \alpha = 4.0^\circ \) (FREE TRANSITION)
Blunt Trailing Edge (T.E. t/c = .01)

Airfoil Pressure Distribution

\( \alpha = 10.7^\circ; \quad R_N = 1.3 \times 10^6 \)

\( M_\infty = 0.201 \)

**FIGURE III-25** - MEASURED PRESSURE DISTRIBUTION ON AIRFOIL SURFACE AND LOCUS OF MINIMUM VELOCITY AT \( \alpha = 10.7^\circ \) for BLUNT TRAILING EDGE AIRFOIL (FREE TRANSITION)
surface and, (ii) to obtain the initial conditions for the purpose of wake flow calculations by the theoretical profile drag method developed under the present studies. Figures IV-1 through IV-4 show the results of measurements of velocity profiles on the upper surface of the airfoil for angles of attack $\alpha$ of 0°, 8°, 11° and 13°, respectively; chordwise locations of measurements are also indicated in these figures. It can be seen from these figures that, for angles of attack of 0° and 8° and for $X/C$ locations forward of or equal to 0.9 in case of $\alpha$ of 8°, the shape of velocity profile is smooth and there is an absence of scatter in the measured values. However, for angles of attack $\alpha$ of 11° and 13°, and in particular for chordwise location close to the trailing edge, the shape of the velocity profile is not smooth near the surface as evidenced by the appreciable scatter in the measured data. The cause of the above phenomena is found in the ability of total-static pressure probe combination to measure reliable velocity profiles under various flow conditions. The pressure probe of Figure III-9 is reliable when the viscous flow measurements are made for attached flow conditions. However, the measurements of velocity profile by this probe, for the boundary layer which is approaching separation or in separated flow region, are not reliable and are subject to interpretations. Hence, the measurements by this probe design under the above-mentioned conditions can be used for the purpose of qualitative analysis only. However, in order to determine the position of initial separation and the extent of separated flow region, measurements performed by the present design of the pressure probe can be effectively used for this purpose by the procedure such as shown in Figure IV-5. Various investigators have observed from experimental measurements of velocity profiles that the value of the form factor $H$ at initial separation point for ordinary turbulent boundary layer is greater than approximately 2.0 and that exact value of $H$ at separation depends upon surface roughness. Thus, in order to determine the initial separation as well as the extent of the separation region from pressure probe measurements, growth rate of integral thickness, such as the displacement or momentum thickness, needs to be examined in addition to Form Factor $H$. This is done in Figure IV-5 and the following conclusion can be drawn from this figure:

1. Separation at T.E. starts at $\alpha = 8.0°$
2. Separation at $X/C = 0.9$ starts at $\alpha = 11.5$ to $12°$
3. Separation at $X/C = 0.8$ starts at $\alpha = 13.0°$

Figures IV-6, IV-7 and IV-8 show the plots of velocity profile measured by the hot wire anemometer technique. These velocity profiles were measured on the upper surface of the airfoil with the blunt trailing edge of 1 percent thicknesses. Angles of attack corresponding to Figures IV-6, IV-7 and IV-8 are 4°, 10.8° and 13° respectively. It can be seen from these figures that measurements of velocity profiles by hot wire anemometer are smooth and reliable for attached boundary layer flow as well as for viscous flow approaching separation. Hot wire anemometer technique is unable to give any indication about the direction of the flow in the boundary layer; i.e., it gives indication only of the absolute magnitude of velocity. In addition, as the magnitude of the flow velocity approaches zero at a point in the flow, the relative magnitude of the velocity of fluctuation is large compared to the mean flow velocity at that point. Under this circumstance the accuracy of measurements by the hot wire anemometer techniques suffer as low...
**Figure IV-1** - Velocity profile measurements on upper surface of airfoil at $\alpha = 0^\circ$ and for sharp trailing edge.
FIGURE IV-2 - UPPER SURFACE PROFILE MEASUREMENTS ON TEST AIRFOIL WITH SHARP TRAILING EDGE AT $\alpha = 8^\circ$ (FREE TRANSITION)
\[ \frac{x}{C} = 0.82 \]
\[ S^* = 0.1767'' \]
\[ \theta = 0.0975'' \]
\[ H = 1.75 \]

\[ \frac{x}{C} = 0.9 \]
\[ S^* = 0.2894'' \]
\[ \theta = 0.1578'' \]
\[ H = 1.834 \]

\[ \frac{x}{C} = 0.985 \]
\[ S^* = 0.4506'' \]
\[ \theta = 0.2003 \]
\[ H = 2.24 \]

\[ C_{REF} = 2.54 \text{ Centimeters} \]

**Figure IV-3** - Velocity Profile Measurements on Upper Surface of Sharp T.E. Test Airfoil at \( \alpha = 11^\circ \) (Free Transition)
$C_{REF} = 2.54$ Centimeters

$X/C = 0.820$
$S^* = 0.3598$
$\theta = 0.1663$
$H = 2.156$

$X/C = 0.900$
$S^* = 0.4789$
$\theta = 0.1793$
$H = 2.670$

$X/C = 0.985$
$S^* = 0.6787$
$\theta = 0.2423$
$H = 2.800$

**Figure IV-4 - Velocity Profile Measurements on Upper Surface of Sharp T.E. Test Airfoil at $\alpha = 13^\circ$ (Free Transition)**
THE FOLLOWING CONCLUSIONS ARE MADE:

(i) SEPARATION AT T.E. STARTS AT $\alpha = 8.0^\circ$
(ii) SEPARATION AT $x/c=0.9$ FOR $\alpha = 11.5$ TO $12^\circ$
(iii) SEPARATION AT $x/c=0.8$ FOR $\alpha = 13.0^\circ$

$C_{REF} = 2.54$ Centimeters

$\alpha \sim$ ANGLE OF ATTACK - DEGREES

FIGURE IV-5 - PLOT OF DISPLACEMENT THICKNESS VS. ANGLE OF ATTACK AT THREE CHORDWISE LOCATIONS FOR TEST AIRFOIL (UPPER SURFACE)
$M_\infty = 0.208$

$R_N = 1.3 \times 10^6$

$\alpha = 4.0^\circ$

$X/C = 0.822$

$X/C = 0.905$

$X/C = 0.965$

$C_{REF} = 2.54$ Centimeters

T.E. t/c = 0.01

**FIGURE IV-6 - EXPERIMENTAL VELOCITY PROFILES BY HOT WIRE ANEMOMETER AT $\alpha = 4.0^\circ$ ON THE UPPER SURFACE OF AIRFOIL WITH BLUNT TRAILING EDGE**
FIGURE IV-7 - EXPERIMENTAL VELOCITY PROFILES BY HOT WIRE ANEMOMETER AT $\alpha = 10.8^\circ$
ON THE UPPER SURFACE OF AIRFOIL WITH BLUNT TRAILING EDGE
FIGURE IV-8 - EXPERIMENTAL VELOCITY PROFILES BY HOT WIRE ANEMOMETER AT \( \alpha = 13.0^\circ \)
ON THE UPPER SURFACE OF AIRFOIL WITH BLUNT TRAILING EDGE
values of velocities are approached in a turbulent flow with reverse flow velocity profiles. Because of the above two reasons, it is necessary to exercise some judgment in fairing the velocity profile shape when measurements are made with the hot wire anemometer techniques in a boundary layer flow having reverse flow velocity profiles. Moreover, large numbers of data points, in the vicinity of values of y locations where the zero velocity is approached, would make it possible to exercise sounder judgment in fairing reverse flow velocity profiles near the y locations when the values of velocity are near zero and near the wall where negative velocities are encountered. An example of this type of fairing is shown in Figure IV-8 for value of $X/C = 0.965$.

An example of the type variations of velocity profile in the wake of the test airfoil with sharp trailing edge is shown in Figure IV-9. These wake surveys correspond to an angle of attack $\alpha$ of $8^\circ$ and when transition on the lower and upper surfaces of the airfoil were fixed at 10 percent chord by the use of grit strips. The wake surveys shown in this figure were performed by the use of total-static pressure probe combination shown in Figure III-9. Near the trailing edge the rate of increase in minimum velocity $U_w$ with respect to the distance $X'/C$ is quite large compared to the large distance downstream from the trailing edge. This figure also points out that the rate of variations in the width of the wake is small at a far distance downstream compared to near the trailing edge. It should be mentioned that this figure is not plotted to give true relative y locations for velocity profiles at different $X'/C$ locations but to indicate relative shape of the velocity profiles at different $X'/C$ locations in the wake behind the airfoil.

Figure IV-10 shows the example of experimentally measured variations of momentum thickness and form factor in the wake behind the sharp trailing edge test airfoil. The data shown in this figure are for an angle of attack of $8^\circ$, for conditions of free transition on the airfoil surfaces and for free stream Reynolds number of approximately 1.3 million. It can be seen from this figure that values of momentum thickness and form factor decrease in the downstream direction in the airfoil wake and at far distance downstream in the wake their values become approximately constant.

Examples of typical measurements of static pressure distributions across the airfoil wake behind the sharp trailing edge test airfoil are shown in Figures IV-11, IV-12 and IV-13; corresponding measurements of velocity profiles are also shown in these figures. Figure IV-11 is for angle of attack $\alpha = 0.0^\circ$, IV-12 is for $\alpha = 8.0^\circ$ and fixed transition on upper and lower surfaces of the airfoil; and Figure IV-13 is for $\alpha = 10.8^\circ$. Measurements shown in these figures indicate that static pressure is not constant across the wake but has an approximate parabolic variation. These figures indicate that static pressure has a higher value at lower edge of wake and lower value at the upper edge of wake for angles of attack greater than zero degree. In addition, it is seen from these figures that the maximum value of the static pressure in the wake occurs in the vicinity of y location corresponding to minimum velocity point in the wake velocity profile.

Figures IV-14, IV-15 and IV-16 show indirect measurements of shear stress profiles at angles of attack of $0^\circ$ and $10.8^\circ$ and at few chordwise locations in the wake behind the airfoil trailing edge. These shear stress profiles are
ALPHA = 8°

FIXED TRANSITION AT 10% CHORD

$C_{REF} = 2.54$ Centimeters

FIGURE IV-9 - WAKE SURVEY FOR SHARP TRAILING EDGE TEST AIRFOIL WITH FIXED TRANSITION AND ANGLE OF ATTACK OF 8°
FIGURE IV-9 - WAKE SURVEY FOR SHARP TRAILING EDGE TEST AIRFOIL WITH FIXED TRANSITION AND ANGLE OF ATTACK OF 8° (Continued)
Symbols are experiment:

- $\triangle$ Total Momentum Thickness
- $\bigcirc$ Lower Form Factor

**Figure IV-10 - Momentum Thickness and Form Factor Distribution in the Wake of Sharp T.E. Test Airfoil at Angle of Attack of 8° (Free Transition)**

- $M_\infty = 0.208$
- $R_N = 1.3 \times 10^6$
- $\alpha = 8.0^\circ$
FIGURE IV-11 - VARIATION OF MEASURED STATIC PRESSURE DISTRIBUTION AND VELOCITY PROFILE ACROSS THE WAKE OF SHARP T.E. TEST AIRFOIL AT $\alpha = 0.0^\circ$
FIGURE IV-12 - VARIATION OF MEASURED STATIC PRESSURE DISTRIBUTION AND VELOCITY PROFILES ACROSS THE WAKE OF SHARP T.E. TEST AIRFOIL AT $\alpha = 8^\circ$
FIGURE IV-13 - VARIATION OF MEASURED STATIC PRESSURE DISTRIBUTION AND VELOCITY PROFILE ACROSS THE WAKE OF SHARP T.E. TEST AIRFOIL AT $\alpha = 10.79^\circ$
FIGURE IV-14 - COMPUTED SHEAR STRESS DISTRIBUTION AND EXPERIMENTAL VELOCITY PROFILE IN THE WAKE OF SHARP T.E. TEST AIRFOIL AT $\alpha = 0.0^\circ$
FIGURE IV-15 - COMPUTED SHEAR STRESS DISTRIBUTION AND EXPERIMENTAL VELOCITY PROFILE IN THE WAKE OF SHARP T.E. TEST AIRFOIL AT $\alpha = 10.79^\circ$
FIGURE IV-16 - COMPUTED SHEAR STRESS DISTRIBUTION AND EXPERIMENTAL VELOCITY PROFILE
IN AIRFOIL WAKE AT \( \alpha = 10.8^\circ \) (\( X_1/C = 0.344; \ X_2/C = 0.517 \))
computed by the numerical solution of integro-differential equation (III-17) in which the boundary conditions of experimentally measured velocity profiles and static pressure distribution is used. These figures show some interesting results such as the value of the shear stress $T(y)$ is maximum (or minimum) at the y location in the wake velocity profile where the velocity has minimum value. Classical theories such as Boussinesq's eddy viscosity concept and Prandtl's mixing length theory would, on the other hand, indicate a value of zero shear stress on the locus of minimum velocity point. As a matter of fact, the value of shear stress, at a y location corresponding to the locus of minimum velocity, can become as high as 8 to 10 percent of free stream dynamic head as can be seen from these figures. The value of the shear stress on the minimum velocity locus is used as one of the boundary conditions in the theoretical equations in the present method for the calculations of the wake flow and the profile drag of a given airfoil. Hence, the use of Boussinesq's or Prandtl's hypothesis in the present method may lead to erroneous calculation results.

IV.2 Investigations of Fundamental Parameters for Wake Flow

Theoretical equations in Section II contain such terms as (i)

$$K_1 \int f(\eta) \, d\eta, \quad K_1 \int f^2(\eta) \, d\eta,$$

(ii) shear integral terms $\int \tau/\rho \, dy$ and shear distribution $\tau(y_0)/\rho$ on the locus of minimum velocity and (iii) integral for pressure distribution across the wake $\int C_p \, dy$, distribution of static pressure on the loci for outer and inner edges of wake and also on the locus of the minimum velocity. In order to solve these theoretical equations for the calculations of flow in the wake and prediction of profile drag of airfoil sections, it is necessary to express the above items by auxiliary equations as functions of either dependent variables or in terms of initial conditions in the vicinity of trailing edge. In this way the number of dependent variables, which are required for the complete specification of flow in the wake, are kept the same as the number of theoretical equations. These auxiliary equations are derived, as shown in the following paragraphs, by the use of dimensional analysis, investigation of generalized parameters and by the use of available results for "similar flows". For this purpose use is also made of experimental measurements obtained during the present investigation.

IV.2.1 Functional Relationships for Velocity Profile Similarity for Wake Flow: Figures IV-17 and IV-18 are the plots of experimental data for similarity of velocity profiles with different similarity parameters. Experimental data used in constructing these similarity curves are for chordwise locations in the wake which are far from the airfoil trailing edge. The same experimental data
FIGURE IV-17 - VELOCITY PROFILE SIMILARITY (1) IN REGION IV
(FAR FROM AIRFOIL TRAILING EDGE)

\[ f(\eta_1) = \frac{1}{2} \{1 + \cos(\eta_1^{0.884} \pi)\} \]

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FIGURE IV-18 - VELOCITY PROFILE SIMILARITY (2) IN REGION IV (FAR FROM AIRFOIL TRAILING EDGE)
for velocity profiles are used in constructing similarity curves of Figures IV-17 and IV-18. Comparison of velocity profile similarity plots of Figures IV-17 and IV-18 indicate that, when similarity parameter shown in Figure IV-18 is used for the purpose of obtaining velocity profile similarity there is less scatter in experimental data than by the use of similarity parameter of Figure IV-17. This fact suggests that similarity parameter of Figure IV-17 is more suitable for true representation of wake flow characteristics at large distance downstream in the wake of single component airfoil with sharp trailing edge. For the purpose of making use of this information about velocity profile similarity, it is necessary to obtain an analytical expression for the relation between similarity parameter \( \eta \) and similarity function \( f(\eta) \). The curve which fits the experimental data of Figure IV-17 has the analytical expression of the form given by

\[
f(\eta_1) = \frac{1}{2} [1 + \cos (\eta_1^{0.884} \cdot \pi)]. \tag{IV-1}
\]

An analytical expression for the curve, which fits the experimental data of Figure IV-18, is given as

\[
f(\eta_2) = \frac{1}{2} [1 + \cos (\eta_2^{0.885} \cdot \pi)]. \tag{IV-2}
\]

Equation (IV-1) and (IV-2) show the functional relationships for the similarity parameters for the viscous flow in the wake at chordwise locations far away from the airfoil trailing edge. Similarity plots for velocity profiles for the viscous flow in the wake of airfoil with sharp trailing edge and in the vicinity of airfoil trailing edge are shown in Figures IV-19 and IV-20. Definitions for similarity parameters and variables in Figures IV-19 and IV-20 are similar to those of Figures IV-17 and IV-18, respectively. As seen in Figure IV-19, the scatter of experimental data for wake flow in the neighborhood of trailing edge is larger than corresponding plot for far distance wake flow shown in Figure IV-17; the similar remark can also be made for data shown in Figure IV-18 and IV-20 which are for large and small distances in the wake behind the airfoil trailing edge. An analytical expression for the curve fit of the mean curve shown in Figure IV-19 is given by

\[
f(\eta_3) = \frac{1}{2} [1 + \cos (\eta_3^{0.825} \cdot \frac{\pi}{2})]. \tag{IV-3}
\]

As the scatter in experimental data, shown in Figure IV-20 is larger than corresponding similarity plots of Figure IV-18, three different curve fits are defined by an expression of the following form:

\[
f(\eta_4) = \frac{1}{2} [1.0 + \cos (\eta_4^{P} \cdot \frac{\pi}{2})]. \tag{IV-4}
\]

where, for the curve \( C_1 \), \( P = 1.0 \),

- for the curve \( C_2 \), \( P = 0.755 \),

and for the curve \( C_3 \), \( P = 0.631 \).

Various values of powers \( P \) can be tried in the theoretical method in order to find out desirable value for better overall correlation with experimental
FIGURE IV-19 - VELOCITY PROFILE SIMILARITY IN THE WAKE IN THE VICINITY OF TRAILING EDGE OF SINGLE-COMPONENT AIRFOIL WITH SHARP TRAILING EDGE

\[ f(\eta_3) = \frac{1}{2} \left[ 1 + \cos(\eta_3^{825} \pi) \right] \]
data. The reason for the larger scatter in experimental data for similarity curves of Figures IV-19 and IV-20 is that these measurements were performed by the use of total-static pressure probe combination in the region where the circulatory flow may be present. This is due to the mixing of upper and lower surface boundary layer and the airfoil trailing edge thickness—even though it is small it has a finite value. The measurements by the total-static pressure probe, such as shown in Figure III-9, in circulatory flow are not very accurate.

IV.2.2 Generalized Parameters for the Pressure Distribution in the Wake of Single Component Airfoil with Sharp Trailing Edge: Figures IV-21 through IV-24 show the plots of non-dimensional pressure or velocity distribution at upper and lower edges of the wake for angles of attack \( \alpha \) of 0.0°, 8.0°, 8.0° (fixed transition) and 10.79°, respectively. Parameter for the non-dimensional pressure at the edges of the wake is defined as

\[
\gamma = \left( \frac{U_e}{U_\infty} \right)(x) - \left( \frac{U_e}{U_\infty} \right)_{T.E.} \quad (IV-5)
\]

\[
1.0 - \left( \frac{U_e}{U_\infty} \right)_{T.E.}
\]

where \( U_e = \) velocity at edges of wake
\( U_\infty = \) freestream velocity

and subscripts T.E. = values at or in the neighborhood of trailing edge over airfoil surface, and

\((x) = \) chordwise locations in the wake.

Non-dimensional parameter \( \gamma \) was chosen because, as seen from equation (IV-5), its value along the wake will vary from zero at the trailing edge to the value of 1.0 at very far distance from the trailing edge. Experimental data, shown in Figures IV-21, IV-22, IV-23 and IV-24, indicate that when values of \( \gamma_u \) or \( \gamma_l \) are plotted against distance \( X'/C \), measured from the airfoil trailing edge, in the wake then experimental data for upper wake and lower wake arrange nicely to form smooth curves for all angles of attack shown. However, different curves are obtained for upper and lower edges of wake and moreover curves of \( \gamma \) vs. \( X'/C \) are different for various angles of attack. Thus, in this form \( \gamma \) vs. \( X'/C \) relationship cannot be utilized as an ingredient to the theoretical drag computation method. By the use of physical reasoning and dimensional analysis, a transformed \( X'/C \) coordinate for the wake flow was arrived at, such that when \( \gamma_u \) or \( \gamma_l \) is plotted versus transformed coordinate along the wake, then experimental data, for both upper and lower edge of wake and for all angles of attack, arranged on a single curve. This dimensionless transformed \( X \)-coordinate is given by the following equation:
FIGURE IV-20 - VELOCITY PROFILE SIMILARITY IN THE WAKE IN THE NEIGHBORHOOD OF TRAILING EDGE OF SINGLE-COMPONENT AIRFOIL WITH SHARP TRAILING EDGE.
FIGURE IV-21 - NONDIMENSIONAL PRESSURE DISTRIBUTION AT
UPPER AND LOWER EDGES OF WAKE FOR \( \alpha = 0.0 \)
**FIGURE IV-22 - Nondimensional Pressure Distribution at Upper and Lower Edges of Wake for $\alpha = 8.0^\circ$ (Free Transition)**

- Upper Edge of Wake
- Lower Edge of Wake
FIGURE IV-23 - NONDIMENSIONAL PRESSURE DISTRIBUTION AT UPPER AND LOWER EDGES OF WAKE AT $\alpha = 8.0^\circ$ (FIXED TRANSITION)
FIGURE IV-24 - NONDIMENSIONAL PRESSURE DISTRIBUTION IN WAKE
AT UPPER AND LOWER EDGES AT $\alpha = 10.79^\circ$
\[ X_U = \frac{X'}{c} \cdot \left[ 1.0 - 12.5 \left( \frac{\delta_G}{\delta_u} \right)^{0.262} \cdot \left\{ \frac{\delta_u - \delta_L}{c} \right\}_{T.E.} \right] \] (IV-6)

or \[ X_L \]

where \( X = \) transformed \( x \)-coordinate along the wake

\( X' = \) distance along wake measured from the airfoil trailing edge

\( c = \) airfoil chord

\( \delta_u = \) boundary layer thickness at the trailing edge on upper surface of airfoil

\( \delta_L = \) boundary layer thickness at the trailing edge on lower surface of airfoil

\( \delta_G = \) greater value of \( \delta_u \) or \( \delta_L \)

Figure IV-25 shows the plot of dimensionless pressure distribution at the edges of the wake versus dimensionless transformed coordinate \( X \). This figure shows that all points at different chordwise locations and various angles of attack arrange nicely on a single curve. The functional relationships between parameters \( \gamma \) and \( \chi \) is given by

\[ \gamma_u = 1 - \frac{0.132}{(0.363 + X_u)^2} \]

or \[ L \]

Figure IV-26 shows the generalized parametric representation and functional relationship for the pressure distribution on the locus of minimum velocity in the airfoil wake for sharp trailing edge airfoil. The parameters for this universal pressure distribution along the locus of minimum velocity point were arrived from the consideration of the flow behind the backward facing step and from physical reasonings. In this case also, by the choice of proper transformed \( X \)-coordinate, experimental points for \( C_{p, \text{min}} \) arrange themselves very nicely on a single curve. The functional relationship between the parameters for the pressure along the locus of minimum velocity in the airfoil wake behind the sharp trailing edge airfoil is given by,

\[ \frac{C_{p, \text{min}}(x)}{C_{p, \text{T.E.}}} = 0.556 e^{-0.6268} + (0.444 + 1.1218 + 0.2858^2)e^{-28} \] (IV-8)

where \( C_{p, \text{min}}(x) = \) static pressure coefficient along the locus of minimum velocity in wake

\( C_{p, \text{T.E.}} = \) pressure coefficient at the trailing edge of airfoil

\( \beta = \) transformed \( x \) coordinate for \( C_p \) along locus of minimum velocity
\[ \gamma = 1 - \frac{0.132}{(0.363 + \chi)^2} \]

\[ \chi = \frac{U_{e\alpha} - U_{e\omega}}{U_{\infty} - U_{e\omega}} \]

\[ \chi = \frac{U_{e\omega} - U_{e\omega}}{U_{\infty} - U_{e\omega}} \]

\[ X = \frac{X}{C} \left[ 1 - 12.5 \left( \frac{\delta_g}{C} \right) \left( \frac{\delta_{u} - \delta_{l}}{C} \right)_{TE} \right] \]

\[ \delta = \text{BOUNDARY LAYER THICKNESS} \ (\delta_{u} \text{ or } \delta_{l}) \]

\[ \delta_g = \text{GREATER OF } \delta_{u} \text{ AND } \delta_{l} \text{ AT T.E.} \]

\[ U_{e} = \text{VELOCITY AT EDGE OF WAKE} \]

\[ C = \text{AIRFOIL CHORD} \]

**Figure IV-25** - Dimensionless universal pressure distribution in the wake at its edges for single component airfoil up to incipient separation (sharp T.E. airfoils)
\[ \frac{C_{P_{\text{min}}}}{C_{P_{\text{te}}}} = 0.556 e^{0.62\beta} + (4.44 + 1.121 \beta + 0.258 \beta^2) e^{2\beta} \]

where, 
\[ \beta = C_{P_{\text{te}}} \left( \frac{X}{C} \right) \left( \frac{C}{S_{\text{TOTAL}_{te}}} \right) \]

**FIGURE IV-26 - VARIATION OF \( \frac{C_{P_{\text{min}}}}{C_{P_{\text{te}}}} \) ALONG THE WAKE OF SINGLE COMPONENT AIRFOIL WITH SHARP TRAILING EDGE**
IV.2.3 Generalized Parametric Representation of Shear Stress at Velocity Minimum: The theoretical integral equations of Section II for the solution of viscous flow in airfoil wake, contained coupling terms involving shear stress at the velocity minimum. It was noted in previous paragraphs that velocity profiles in the airfoil wake were "similar" for proper choice of similarity function and similarity variable. This fact suggests that it is possible to arrive at a generalized parameter for the representation of shear at minimum velocity point from Prandtl's new shear stress hypothesis for free jet flows. According to this hypothesis, at any \( X \)-location the turbulent shear stress is given by,

\[
\tau(y) = K_1 \rho_c b_c U_c \frac{d\tau}{dy}
\]

where \( \tau(y) \) = shear stress at any \( y \) ordinate in viscous layer
\( b_c \) = characteristic width of viscous layer
\( U_c \) = characteristic velocity in viscous layer
\( K_1 \) = constant.

For the upper wake the suitable characteristic width is (nomenclature is shown in Figure IV-27),

\[
b_c = (y_4 - y_3)(x)
\]

and the characteristic velocity is

\[
U_c = U_{eU}(x) - U_{min}(x)
\]

For the lower wake, the characteristic width is

\[
b = (y_3 - y_2)(x)
\]

and the characteristic velocity is

\[
U_c = U_{eL}(x) - U_{min}(x)
\]
The locus of the minimum velocity $U_{min}(x)$ is contained in both upper wake and lower wake layers. Assuming that shear at the locus of minimum velocity is proportional to the product of the four quantities given by equations (IV-10), (IV-11), (IV-12) and (IV-13), the following functional representation can be written after non-dimensionalizing,

$$\frac{\tau(y_3)}{\frac{1}{2}\rho U_e^2} = F \left[ \frac{(y_4 - y_3)}{(y_4 - y_2)} \cdot \frac{(y_3 - y_2)}{(y_4 - y_2)} \cdot \frac{U_{eU}(x)}{U_\infty} - \frac{U_{min}(x)}{U_\infty} \cdot \frac{U_{eL}(x)}{U_\infty} - \frac{U_{min}(x)}{U_\infty} \right]$$ (IV-14)

Figure IV-27 shows the functional relationship between the non-dimensional shear on the locus of the minimum velocity and the product of the parameter indicated in equation (IV-14). Experimental data plotted in this figure are obtained during the present studies in the wake of sharp trailing edge test airfoil at various chordwise locations and various angles of attack. Non-dimensional shear $\frac{\tau(y_3)}{(\frac{1}{2}\rho U_e^2)}$ was obtained from the indirect shear measurements from measured experimental velocity profiles and pressure distributions by the aid of the numerical method. The curve fit for the functional relationship of Figure IV-27 is given by

$$\frac{\tau(y_3)}{\frac{1}{2}\rho U_e^2} = -115.2 \theta^2$$ (IV-15)

where $\tau(y_3)$ = shear stress on the locus of minimum velocity

$U_{e}(x)$ = arithmetic mean of velocities at upper and lower edges of wake

and

$$\theta = \left[ \frac{(y_4 - y_3)}{(y_4 - y_2)} \cdot \frac{(y_3 - y_2)}{(y_4 - y_2)} \cdot \frac{U_{eU}(x)}{U_\infty} - \frac{U_{min}(x)}{U_\infty} \cdot \frac{U_{eL}(x)}{U_\infty} - \frac{U_{min}(x)}{U_\infty} \right]$$

IV.3 Presentation of Correlation Results

Computer program subroutines were developed for the purpose of comparing the results of theoretical computation method, developed under this study, with the experimental data for three airfoil configurations. Input to the computer program consists of initial conditions of boundary layer and pressure quantities on upper and lower surfaces of the airfoil in the vicinity of the trailing edge. Experimentally measured values for these input conditions were used as input to the computer program. Computations for the characteristics of the flow in the wake and profile drag were performed at several angles of attack for the following three airfoils: (i) Joukowski airfoil with thickness ratio $t/c$ of 12 percent, (ii) present sharp trailing edge test airfoil with thickness ratio of 15 percent, and (iii) NACA 631-012 airfoil. Results of correlations are discussed in the following paragraphs.
\[ \frac{T(y)}{\frac{1}{2} \rho U_c^2} = 115.2 \Theta^2 \]

\[ \Theta = \left( \frac{x_1}{y} \right) \left( \frac{x_2}{y} \right) \left( \frac{x_3}{u_\infty} \right) \left( \frac{x_4}{u_\infty} \right) \]

where,
\[ x_1 = (y_4 - y_3) \quad x_2 = (y_3 - y_2) \]
\[ x_3 = (U_{eu} - U_{min}) \quad x_4 = (U_{el} - U_{min}) \]
\[ y = (y_4 - y_2) \quad U = \frac{1}{2}(U_{eu} + U_{el}) \]

FIGURE IV-27 - PARAMETRIC RELATIONSHIP OF SHEAR STRESS ON THE LOCUS OF MINIMUM VELOCITY ALONG THE WAKE OF SINGLE COMPONENT AIRFOIL
(i) Joukowski Airfoil: Figure IV-28 shows the results of computations for upper and lower edge of velocity profiles in the wake of Joukowski airfoil at zero degree angle of attack. In this figure the locus of minimum velocity, which in this case is a straight line because of symmetrical airfoil at zero angle of attack, is also shown. Experimental data shown plotted in this figure by symbols indicate that good correlation is obtained.

Figure IV-29 shows the plot of experimental measurements (Reference 7) of boundary layer momentum thickness, displacement thickness and the form factor at the trailing edge of Joukowski's airfoil as a function of an angle of attack. This plot shows that at an angle of attack of 60°, the lower surface boundary layer is separated near the trailing edge as indicated by the value of form factor $H_{lower} = 2.15$. At an angle of 90°, both upper and lower surface boundary layer are separated; this is indicated by the values of form factors and also by sudden increase in slope $d\delta*/da$ for the upper surface at $\alpha = 90°$. These values of physical boundary layer thickness and pressure coefficient at the trailing edge were input to the profile drag computer program. Values of boundary layer physical thicknesses were computed from experimental momentum thicknesses and form factors by the use of the following equation:

$$\frac{\delta}{\theta} = \frac{1 + 0.1 M_e^2}{0.17 \left[1 - \exp \{-3.5(H-1)\}\right]} \quad (IV-16)$$

Figure IV-30 shows the computed variations of displacement thickness, momentum thickness and form factor in the wake of Joukowski airfoil at an angle of attack $\alpha = 0°$. Figure IV-31 shows the plot of above parameters at an angle of attack $\alpha = 60°$. Experimentally measured values of these parameters, which have been shown plotted in Figure IV-30 and IV-31 as symbols, indicate that good agreement is obtained between computational results and experimental data. Figures IV-32 and IV-33 show the comparisons between results of computations of velocity profiles and experimental data in the wake of Joukowski airfoil for angles of attack of 0° and 60°, respectively. The above comparison is made at $X'/C = 0.1, 0.25$ and 0.5 in Joukowski airfoil wake and this comparison between computed and experimental velocity profiles indicate that reasonable correlation is obtained.

Figure IV-34 shows the plot of computed values of the profile drag coefficients as a function of an angle of attack and comparison with experimental data. This figure shows that the comparison between computed profile drag values and experimental data is reasonable at angles of attack $\alpha = 0°$, $3°$ and $6°$ whereas at $\alpha = 9°$ the comparison between present theoretical computational results and experimental data is not so good. Experimental measurements at airfoil trailing edge, which is shown in Figure IV-29, indicate that at $\alpha = 0°$ and $3°$ flow separation is absent on airfoil surfaces. Incipient separation exists on airfoil surfaces near the trailing edge in the neighborhood of $\alpha = 6°$ and appreciable flow separation probably exists on airfoil surfaces for angles of attack greater than $\alpha = 6°$ as evidenced from Figure IV-29. The discrepancy in drag between results of computations and experimental drag data for angles of attack greater than $6°$ can be due to the following two reasons. The initial conditions of boundary layer quantities, which are required as input to computer program were obtained from data of Figure IV-29. These data, which were obtained from Reference 7, were measured by pressure probe and the validity
FIGURE IV-28 - LOCI OF VARIOUS EDGES FOR VELOCITY PROFILES IN THE WAKE BEHIND JOUKOWSKI AIRFOIL AT $\alpha = 0^\circ$
\( \Delta \) Momentum Thickness \( \Theta_{T.E.}, \frac{\Theta}{c}_{T.E.} \); \( \odot \) Displacement Thickness \( \Theta_{T.E.}, \frac{\Theta}{c}_{T.E.} \)

\( \square \) Form Factor at T.E., \( \frac{\Theta}{c}_{T.E.} \)

Figure IV-29 - Variation of \( \frac{\Theta}{c}, \frac{\Theta}{c}, \) and Form Factor at Trailing Edge of Joukowski Airfoil with Angle of Attack
FIGURE IV-30 - VARIATION OF $\frac{\delta_s}{c}$, $\frac{\Omega}{c}$ AND $h$ IN THE WAKE OF JOUKOWSKI AIRFOIL
$\alpha = 0^\circ$ AND $C_D$
COMPARISON WITH EXPERIMENTAL DATA
Figure IV-31 - Computed variation of $\frac{\partial}{\partial} \text{ and } H$ in the wake of Joukowski airfoil $\alpha = 6^\circ$ and comparison with experimental data.
FIGURE IV-32 - COMPUTED VELOCITY PROFILES IN THE WAKE OF JOUKOWSKI AIRPOIL AT $\alpha = 0^\circ$ AND COMPARISON WITH EXPERIMENTAL MEASUREMENTS
FIGURE IV-33 - COMPUTED VELOCITY PROFILES IN THE WAKE OF JOUKOWSKI AIRFOIL @ $\alpha = 6.0^\circ$ AND COMPARISON WITH EXPERIMENTAL DATA
FIGURE IV-34 - COMPUTED VARIATION OF PROFILE DRAG COEFFICIENTS WITH ANGLE OF ATTACK FOR JOUKOWSKI AIRFOIL AND COMPARISON WITH EXPERIMENTAL MEASUREMENTS
of measurements by the pressure probe in the separated boundary layer is questionable. Secondly, the validity of assumptions which were used in deriving theoretical wake flow equations of Section II, when appreciable flow separation exists on airfoil surfaces, is not known at the present time. For the purpose of clarifying the above-mentioned situation, need exists for (i) developing proper experimental techniques for obtaining accurate and reliable measurements in separated flow region and (ii) then making appropriate modifications to the theory for wake flow calculations as are necessary for accurate drag predictions in the presence of flow separation.

(ii) Sharp T.E. Test Airfoil: Figure IV-35 shows the plot of computed characteristic loci at an angle of attack \( \alpha = 8^\circ \) and for the case of free transition on the surfaces of the test airfoil. Computed loci are plotted for upper edge of the wake, lower edge of the wake and the locus of minimum velocity for the velocity profiles in the test airfoil wake. Experimental values, which are shown in this figure as symbols, indicate that agreement between computed values for these characteristic points in velocity profiles and experimental data is good. It should be noted that slope of these loci downstream of \( \chi'/C \) of approximately 0.5 become approximately parallel to the free stream direction.

Figures IV-36, IV-37 and IV-38 show the plots of computed values of momentum thickness and form factor distribution in the wake of the test airfoil at angles of attack \( \alpha = 0^\circ, 8^\circ \) (fixed transition) and 10.79°, respectively. Computed values of momentum thickness distribution is shown plotted for lower half of wake profile and total wake velocity profile; difference between these two momentum thickness distributions is the upper wake momentum thickness. Computational results for form factor distribution in airfoil wake indicate that even though the form factor on the upper and lower surfaces at the trailing edge are vastly different, the values of form factors for upper and lower half of wake velocity profile become identical at a very short distance from the airfoil trailing edge. For this reason only one value of computed form factor distribution for the flow in the wake is shown in Figures IV-36, IV-37 and IV-38. Experimental data for the form factor distributions, which are shown plotted in this figure, are either for lower half wake or upper half wake because measured values of form factors are very nearly equal for either upper or lower wake velocity profile. Figure IV-39 shows the plot of results of calculations of profile drag versus angle of attack for the test airfoil. Experimental data which are shown plotted in this figure indicate that agreement between experiment and theoretical method is reasonable.

(iii) NAC 631-012 Airfoil - Results of correlation between computations and experimental data for this airfoil are shown in Figure IV-40. Experimental data shown in this figure are obtained from Reference 5. For the purpose of correlation for this airfoil, measured values of boundary layer quantities on airfoil upper surface at trailing edge were used as input to the profile drag computer program. Boundary layer quantities on the lower surface at the trailing edge of the airfoil which are also required input to the computer program, were obtained from the output of the computer program subroutine for ordinary boundary layer calculation method. Experimental pressure distributions on the lower surface of the airfoils were used as an input for the purpose of calculation of lower surface boundary layer quantities at several angles of attack. In addition, values of experimental pressure at the trailing edge of
FIGURE IV-35 - COMPARISON OF COMPUTED AND EXPERIMENTAL CHARACTERISTIC LOCI FOR $\alpha = 8^\circ$ (FREE TRANSITION) FOR SHARP T.E. TEST AIRFOIL
$\alpha = 0^\circ$

Computation; Symbols are Experiments

- Total Momentum Thickness
- Lower Half Momentum Thickness
- Form Factor

Figure IV-36 - Computed variation of $\Theta/C$ and $H$ in the wake of sharp T.E. test airfoil $\Theta = 0^\circ$ and comparison with experimental measurements.
FIGURE IV-37 - COMPUTED DISTRIBUTION OF $\Theta/C$ AND $H$ IN THE WAKE OF SHARP T.E. TEST AIRFOIL AT $\alpha = 8.0^\circ$ (FIXED TRANSITION) AND COMPARISON WITH EXPERIMENTAL DATA
FIGURE IV-38 - MOMENTUM THICKNESS AND FORM FACTOR DISTRIBUTIONS IN THE WAKE OF SHARP T.E. TEST AIRFOIL AT $\alpha = 10.79^\circ$ (FREE TRANSITION) AND COMPARISON WITH EXPERIMENTAL DATA
FIGURE IV-39 - COMPARISON OF $C_D$ VS. $\alpha$ BETWEEN THEORETICAL METHOD AND EXPERIMENTAL DATA FOR SHARP T.E. TEST AIRFOIL.
$H = \text{FORM FACTOR AT TE FOR UPPER SURFACE}$

**LIFT COEFFICIENT - $C_L$**

- $H = 1.95$
- $2.1$
- $2.2$
- $2.4$
- $2.6$

**PROFILE DRAG COEFFICIENT - $C_D$**

- NASA TN 2502
- SYMBOLS ARE EXPERIMENTS
- COMPUTATIONS

**ANALYSIS**

- Figure IV-40 - PROFILE DRAG CORRELATION FOR NACA 631-012 AIRFOIL
the airfoil were used as input to the profile drag computer program subroutines. Figure IV-40 shows the comparison of the results of computations for the profile drag coefficient of NACA 631-012 airfoil with experimental data. Plot of experimental $C_L$ vs. $\alpha$ for this airfoil is also shown in this figure; in addition, values of form factor on the upper surface of the airfoil at its trailing edge are noted on this $C_L - \alpha$ curve at several angles of attack. This is done for the purpose of indication of approaching trailing edge separation and also for the purpose of indicating values of angles of attack for which separated flow conditions exist at airfoil trailing edge. The comparison of computed profile drag quantities with experimental data is good as seen from this figure.

V. CONCLUSIONS AND RECOMMENDATIONS

From the theoretical and experimental studies presented in this report it is possible to make the following conclusions and recommendations for the future studies.

V.1 Conclusions

(1) The total-static pressure probe combination, such as used in the present studies, can be used for reliable measurements in viscous flow over airfoil surface when this flow is attached or non-separated. However, the measurements by such a probe, for the flow approaching separation or in separated flow region, are subject to interpretation and hence can be used only in qualitative sense.

(2) Measurements of velocity profile by the use of hot-wire anemometer technique have been found more reliable, both qualitatively and quantitatively, than similar measurements performed by the use of pressure probe. However, with the present state of the art in hot-wire anemometry technique, measurements of velocity profiles in the region of low values of velocities (less than 30 fps and nominal values of turbulence level) are not very accurate. As the hot-wire anemometer measures only the absolute magnitude of velocity then with the above-mentioned present limitation, it is difficult to determine the separated flow velocity profile with the desired accuracy.

(3) Shear stress profiles for the wake flow have been determined by the use of indirect measurements and these values are used in the parametric form in the present theoretical method. The accuracy of indirect shear measurements for the wake flow in the trailing edge region is questionable because of the existence of the circulatory shear flow in this region. For this reason it is recommended to perform the direct shear measurements by the use of such devices as hot-wire X probe or by the use of Laser Doppler Velocimeter and make comparisons with the present indirect shear measurements.
(4) From the comparison between results of computer program subroutines with experimental data on three airfoil configurations, the following three remarks can be made:

(i) Agreement between computed and experimental velocity profiles for the flow in the wake is quite good for symmetrical wake at large distances from the trailing edge. However, for the unsymmetrical wake and especially in the region of wake near the airfoil trailing edge, the discrepancies exist between the results of computations and experimental measurements. These discrepancies might be due to inaccuracies in measurements or theoretical calculations or both in this region.

(ii) The computed variations of integral quantities in the airfoil wake, such as momentum thickness, displacement thickness and the form factor, agree well with experimental data for distances far from the trailing edge. In the region near the trailing edge, however, discrepancies are observed between the theory and experiments.

(iii) Results of computations of profile drag agree quite well with experimental measurements in the range of values of angles of attack from $\alpha = 0^\circ$ to $\alpha$ corresponding to the occurrence of incipient separation on the upper surface of the airfoil. However, for high values of angles of attack where large region of flow separation is present on the airfoil surface, computed values of the profile drag are not in good agreement with experimental measurements. This discrepancy can be attributed to the breakdown of the assumptions, in case of separated flow, which were used in deriving theoretical equations for the present method.

V.2 Recommendations

(1) For the purpose of establishing the limitations and restrictions of the present method, it is necessary to perform correlations with experimental data for the wake flow and profile drag on several classes of airfoil configurations. Thus performing calculations on supercritical and conventional airfoils of several thickness ratios and camber distributions and at several values of lift coefficients and comparison of computational results with experimental measurements would be very valuable for the purposes of needed refinements for this new method.

(2) This method is developed for sharp trailing edge airfoils and for low subsonic free stream Mach number where the flow can be considered essentially incompressible. However, for the general applicability in the design and development work on wing sections it is recommended that the present method be extended for high subsonic flows and for airfoils with thick trailing edges.

(3) Experimental and theoretical studies are recommended for the refinements of the present method for the purpose of the prediction of profile drag when extensive region of flow separation is present on airfoil surface. Such conditions exist at $C_{L_{\text{MAX}}}$ conditions on airfoils exhibiting trailing edge stall.
The basic approach of the theory of the present method is valid for the more important situations of the computation of profile drag for multi-component airfoil sections. Under the present study the validity of this approach is proven for single-component airfoil sections. It is hence recommended that this approach be extended to airfoils with more than one component.
REFERENCES


It is known that in the case of two-dimensional testing at conditions of high lift, the validity and accuracy of experimental measurements is sometimes questionable because of the existence of boundary layer separation on the test section sidewalls. The presence of the two-dimensional model at moderate and high angles of attack in the wind tunnel test section creates high adverse pressure gradients in the direction of flow on the side walls of the tunnel test section upstream of the leading edge of the model. This high adverse pressure gradient causes the separation of boundary layer on the wind tunnel test section side walls at the juncture of the model with the side walls. Separated boundary layer on the side walls then flows toward the center of the model with the result that the flow pattern on the airfoil model is three-dimensional thus making it impossible to obtain measurements of two-dimensional aerodynamic characteristics of given airfoil model at moderate and high angles of attack. The above-mentioned phenomena has been observed by various investigators at NASA, R.A.E. in England, and at Lockheed-Georgia Company.

The solution of the above problem can be approached by the use of several methods, such as distributed blowing through slits located in the sidewalls at juncture of the model with side walls, distributed suction through pores or slits located upstream of the model or at the juncture of the model with the tunnel side walls, and blowing high energy air through a single slit located at an appropriate distance upstream of the leading edge of the model on both side walls. For the present investigations boundary layer control on wind tunnel side walls was accomplished by blowing the appropriate amount of high energy air through single slits located at approximately one chord length upstream of model leading edge on both side walls. Figure III-8 shows the general layout of the side wall boundary layer control system used in the present investigation. The following paragraphs describe the procedure that was used in determining the appropriate amount of high energy air for various model configurations and angles of attack.

Velocity profiles on the side walls of the tunnel were measured without the presence of the model at locations corresponding to the approximate locations of the trailing edge of the main component and flap. Then, with the model in the tunnel in various configurations and angles of attack, measurements of velocity profiles were made on the side walls of the tunnel at the same locations. These measurements were used to determine blowing requirements. It is necessary that the side wall BLC be just sufficient to prevent boundary layer separation on the side walls. It has to be emphasized that the amount of blowing on the wind tunnel side walls for boundary layer control has to be precise—for example, if there is excess blowing then the value of freestream in the test section would be affected. In addition, an excess amount of blowing on wind tunnel walls creates an undesirable pressure field around the two-dimensional model; this is due to the fact that vortices are created due to the rubbing action of low velocity tunnel freestream air with the high velocity blown air. These vortices give rise to tip effects or the effects of finite span on two-dimensional test model. If the amount of blown high energy air on the tunnel side walls is
insufficient then, of course, the boundary layer on the side walls separates. In order to avoid both undesirable effects, the proper value of the pressure ratio $P_{ts}$, $P_{ss}$ and the proper geometrical dimensions of the slits are required.

A simplified theoretical equation of the amount of blown air required to suppress boundary layer separation on wind tunnel side walls can be derived as

\[ C_{\mu_{\text{required}}} = \frac{2}{e} \left( \frac{\Delta \theta_{\text{max}}}{c} \right) + \frac{1}{2} \frac{P_s}{\rho_\infty} \frac{h_s}{c} \]  \hspace{1cm} (A-1)

where $\Delta \theta_{\text{max}}$ = maximum value of the difference in momentum thickness on wind tunnel side walls in the presence of and absence of model in the test section, inches

$c$ = airfoil chord, inches

e = effectivity factor

$h_s$ = height of blowing slits, inches

$\rho_s$ = density of high energy air in blowing plenum

$\rho_\infty$ = freestream density.

The blowing momentum coefficient $C_{\mu}$ in equation (A-1) is defined as

\[ C_{\mu} = \frac{\text{mass of blown air}}{\frac{1}{2} \rho_\infty U_\infty^2} \cdot \frac{(\text{velocity of blown air})}{(\text{wing chord}) \cdot (\text{span of model})} \]  \hspace{1cm} (A-2)

The value for the ratio of velocity of blown air through the blowing slit to the freestream velocity, $V_s/U_\infty$, is approximately by

\[ \frac{V_s}{U_\infty} = 2.0 \left( \frac{q_\infty}{\rho_\infty} \right)^{1/2} \left[ \frac{\gamma}{\gamma-1} \cdot g_c \cdot T_0 \left\{ 1 - \left( \frac{P_{ss}}{P_{ts}} \right)^{0.286} \right\} \right]^{1/2} \]  \hspace{1cm} (A-3)

where $\gamma$ = ratio of specific heats for air (1.4)

$R$ = gas constant for air (=53.3)

$g_c$ = gravitational constant (=32.2)

$P_{ss}$ = static pressure at the exit of blowing slit, psia

$P_{ts}$ = plenum air total pressure.

The amount of $C_{\mu}$ that is available corresponding to a given pressure ratio, $P_{ts}/P_{ss}$, is given by the following equation:
\[ C_{\mu \text{ available}} = 2 \left( \frac{\rho_s}{\rho_\infty} \right) \left( \frac{V_s}{U_\infty} \right) \left( \frac{h_s}{c} \right) \frac{K_w}{h_t} \]  \hfill (A-4)

where \( V_s/U_\infty \) is given by equation (A-3)

- \( h_t = \text{span of the model, inches} \)
  - = 30 inches for the present tunnel
- \( K_w = \text{width of blowing slit, inches} \)
  - = 30 inches for the present tunnel

Figure A-1 shows the plot of available \( C_{\mu} \) for the unit slot height as a function of pressure ratio \( P_{ts}/P_{s_\infty} \). Figure A-2 shows the plot of velocity ratio as a function of pressure ratio \( P_{ts}/P_{s_\infty} \). Figure A-3 shows the plot of effectivity factor as a function of velocity ratio \( U_{e_\infty}/V_s \). The curve of Figure A-3 is obtained from knowledge of experimental measurements and is empirical. The curves shown in Figures A-1 and A-2 are plots of equations (A-4) and (A-3), respectively; these curves are constructed for freestream dynamic head \( q_\infty \) of 60 psf and freestream total temperature of 500°F. By simultaneous use of equation (A-1) and the curves of Figures A-1, A-2, and A-3, a desired value of the pressure ratio of blown high energy air can be calculated by trial and error such that the required \( C_{\mu} \) is equal to the \( C_{\mu} \) which is available corresponding to a given pressure ratio \( P_{ts}/P_{s_\infty} \).
Figure A-1 Wind Tunnel B.L.C. Performance
Figure A-2 Velocity Ratio of Blown Air at Slot Exit, as a Function of Pressure Ratio
Figure 4-3  Effectivity Factor Versus Reciprocal of Velocity Ratio at Exit of Slot of Blowing Plenum
APPENDIX B

ADDITIONAL INSTRUMENTATION

The test section flow conditions and wind tunnel performance data were monitored with conventional instrumentation. Fan speed, clutch current, and tunnel airstream total temperature were displayed on the control console near the speed control potentiometer as shown in Figure B-1. During these tests, atmospheric pressure was measured using a 9-inch diameter Wallace and Tieman absolute pressure gage with reading increments to 0.01 pounds per square inch. Atmospheric pressure was manually input to the data system before each run.

The test section dynamic pressure was set and monitored visually with a 70-inch water manometer connected to pressure orifices located upstream and downstream of the wind tunnel contraction. The manometer reading gave the static pressure drop across the contraction. This reading was related by calibration to the dynamic pressure in the empty test section. The manometer, which is shown in Figure B-2 has provisions for reading the height of the water column to within ±0.001 inch with good repeatability. This manometer was also used to calibrate some of the pressure transducers used during these tests.

The contraction pressures were also read in electrical units using two Statham type PM6TC pressure transducers rated at ±1.0 pound per square inch differential. These transducers can be expected to be accurate to within about 0.5 pounds per square foot in the normal operating range. One transducer was connected to the contraction pressure orifices in parallel with the water manometer and was calibrated versus dynamic pressure in the empty test section. The other was connected to the upstream or high pressure end of the contraction and was calibrated versus test section total pressure. The output of these transducers was transmitted to the data acquisition system for recording and use in data reduction.

The wind tunnel airstream total temperature is measured by a thermistor mounted on a probe in the settling chamber. Temperature information from the probe is displayed on the control console and is transmitted electrically to the data acquisition system. A diagram of the basic wind tunnel instrumentation is shown in Figure B-3.

Static pressures were measured at 41 orifice locations on the model using two Statham model D3-GM scanivalves with two PM-131 pressure transducers. The scanivalves were ganged together and actuated by a single solenoid type stepper. Valve number one contained a transducer rated at 12.5 pounds per square inch and was connected to the orifices on the model upper surface. Valve number two contained a 2.5 pounds per square inch transducer and was connected to the orifices on the lower surface of the model. The output from these transducers was recorded by the data acquisition system for use in computing pressure coefficients, lift coefficients, and local velocities. Figure B-4 shows the scanivalve installation.

Total pressure in the boundary layer control plenums was calibrated versus the pressure in the auxiliary air supply duct between the plenums and the control
valve. The supply duct pressure was displayed on an ordinary 0 to 100 pounds per square inch pressure gage mounted on the control console as shown in Figure B-1.
Figure B-1  Wind Tunnel Control Console
Figure B-2  70-Inch Water Manometer
Figure B-3. Wind Tunnel Instrumentation Diagram
Figure B-4  Scanivalve Installation
APPENDIX C
DATA ACQUISITION SYSTEM

General Description

The data processing system utilized for this contract was set up for two specific purposes, these being the measurement of the static pressure distribution on the surface of the model and the measurement of the velocity profiles within the boundary layer and wake of the model. The measurements were made using a Data Acquisition Unit (D.A.U.) which was controlled by a real-time digital computer which activated scanivalve units for obtaining the static pressure distribution and traversed a pressure or hot wire anemometer probe for the velocity profiles.

The system used for acquiring and reducing the test data is shown in Figure C-1. The heart of the system is a Lockheed Electronics MAC 16 computer. The raw data was made available, in an abbreviated form, on a teletype for on-line monitoring of the test, and in its entirety on paper tape using a high speed punch. Final data reduction was accomplished on a UNIVAC 1106 Central Computing System with remote access terminals.

An additional MAC 16 computer was used in an executive mode to permit time sharing of the main computer. The disc storage associated with this computer was intended to provide the basis of an on-line data reduction capability in conjunction with a UNIVAC 418. During this contract, however, the immediate availability of the remote access terminals made the on-line capability unnecessary.

The Data Acquisition Unit

The data acquisition unit incorporated an analog multiplexer for transmission of measured data and a digital multiplexer for transmission of thumb-wheel switch inputs. One additional analog channel was provided for probe position measurement.

A rotary switch permitted any of the measured data to be monitored on a digital voltmeter during the test. Variable gain amplifiers in all of the analog channels enabled gain levels to be selected that approximated the raw data to engineering units for ease of monitoring of the digital voltmeter and on-line teletype output.

The tunnel conditions of $q'$, $H'$ and temperature were acquired at each scanivalve position during the static pressure distribution and at each probe position during the boundary layer survey. The thumb-wheel switch inputs included a probe option for choice of no probe, pressure probe or anemometer probe and also a selection of the number of scanivalves and ports to be cycled through the static pressure distribution. Any selection below the maximum
The Data Acquisition and Reduction System

Figure C-1
required (2 scanivalves and 36 ports) inhibited printout of the pressure data during reduction with significant saving in reduction time. This option was exercised when a series of velocity surveys were made with no change of basic configuration, and hence pressure survey, in which case the minimum number of ports was selected that would include the static pressure port at the chordwise location of the velocity survey.

Control of the data acquisition was achieved with one analog channel which activated the probe positioning servo mechanism and one digital channel which stepped and homed the scanivalves and controlled the multiplexing.