VI. Wave Planarity

Steady-state distortion levels produced during full scale P³G operation as evaluated in terms of IDC (circumferential) and IDR (radial) distortion parameters were generally less 0.02 and nominally 0.01 or less as previously realized in the model test.

Figure 10 summarizes the circumferential planarity in terms of phase and amplitude. The probe readings from two of the rakes, 180° apart, at the engine IGV plane were averaged to obtain these summary results. Such a comparison indicates excellent circumferential planarity over the complete range of the test data.

A similar summary of radial planarity characteristics is presented in Fig. 11. In this case, all the probe readings from 2 selected radial immersions were averaged. Radial immersion B is the tip and D is the hub ring. The hub ring probes had the most deviation from those in any of the other rings, thus the results of Fig. 11 represent the "worst case."

VII. Conclusions

It has been shown that the P³G (Planar Pressure Pulse Generator) a new dynamic distortion generator, has produced discrete frequency pressure waveforms of high quality which are controllable in both amplitude and frequency. This has been accomplished over a wider range of amplitude, frequency and airflow than reported in the literature for other discrete frequency distortion devices.

Application of P³G type devices in component and engine stability testing should permit:

1) More comprehensive experimental isolation and study of unsteady aerodynamic instability phenomena than previously possible.

2) Establishment of a data base for validation of high speed turbine machinery dynamic math models at the higher frequencies of recent concern.

VIII. References

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Evaluation of Hypermixing for Thrust Augmenting Ejectors

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The additional thrust required to give an aircraft VSTOL capability may be obtained by diverting the exhaust of the cruise engine through a thrust augmenting ejector. The hypermixing nozzle has been developed to increase the rate of jet mixing and thereby improve the performance of the ejector. Since this is achieved at some cost in primary thrust efficiency, comparison tests were performed with a single shroud and three interchangeable nozzles. A one-dimensional analysis is used to compute ideal levels of augmentation and assess the relative importance of the injection losses. It is seen that hypermixing significantly improves ejector performance by making efficient diffusion of the mixed flow possible.

Nomenclature

A = area
c_p = specific heat at constant pressure
c_v = specific heat at constant volume
$\dot{m}$ = rate of mass flow
n = transfer efficiency of the mixing process
P = fluid pressure
T = fluid temperature
V = flow velocity
β = measure of profile curvature
η = nozzle thrust efficiency
ρ = fluid density
ϕ = thrust augmentation ratio

Subscripts

$_0$ = ejector inlet condition, primary flow
$_1$ = ejector inlet condition, entrained flow
$_2$ = diffuser inlet condition, mixed flow
$_3$ = diffuser exhaust condition, mixed flow
$*$ = isentropic reference condition
a = ambient condition
t = stagnation condition

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Introduction

AN ejector is a mechanically simple device in which entrainment by a jet of primary fluid is used to pump a secondary flow through a duct. The thrust of the mixed flow generally exceeds that of the primary jet alone. This phenomenon can be used in aircraft applications to augment and deflect the thrust of a cruise engine in order to achieve vertical or very short takeoffs and landings. A practical VSTOL aircraft ejector has been an active research goal for several decades. However, until recently, ejectors that achieved significant levels of augmentation have been too long for aircraft installation. This was a re-

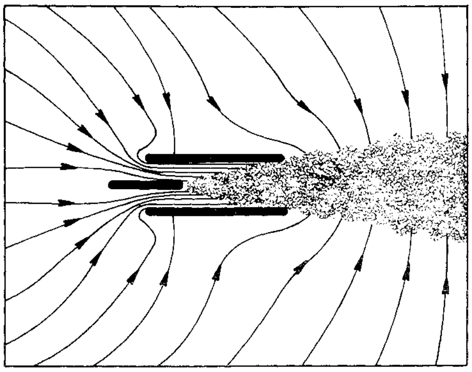

Fig. 1 Streamlines of the entrained flow.

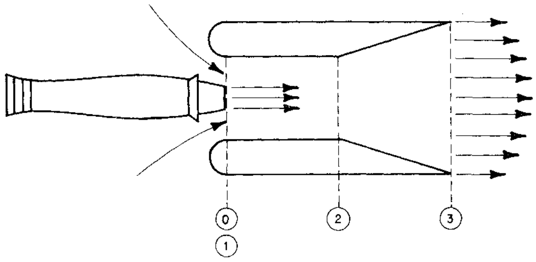

Fig. 2 An elementary thrust augmentor.

sult of the requirement for having a long duct to insure complete mixing of the primary jet and secondary entrained flow.

Although the details of the mixing process have been the subject of considerable research, it is still not clear how entrainment occurs. One hypothesis (see, for example, Bevilaqua and Lykoudis[1]) is that turbulent entrainment is primarily due to the action of large vortices. The mixing rate of the hypermixing jet developed at the Aerospace Research Laboratories is increased by segmenting the exit plane of the nozzle so as to form streamwise vortices. The hypermixing action of these vortices has been discussed by Bevilaqua.[2] In the present work, the effects of hypermixing on a class of low-area ratio ejectors is examined. It is based on Fancher's[3] preliminary study of an ejector intended for use in an augmentor flap.

Analysis of Ejector Performance

The cost of hypermixing is a loss in nozzle thrust efficiency. In order to understand how this loss must be balanced with the other factors that determine performance, the principles of ejector mechanics will be examined using a simple extension of the treatment suggested by von Karman.[4] Consider an elementary ejector, consisting of a primary injection nozzle and a straight walled shroud. The primary jet entrains fluid within the duct and discharges it at the downstream end, thereby establishing a continuous secondary through flow. The streamlines of this motion are shown in Fig. 1. The leading edge singularity which occurs in an ideal fluid produces a net lip thrust on the shroud. It is this force that augments the primary thrust of the jet. Of course, in a real fluid, the flow would separate at the duct entrance and the lip thrust would not develop. The importance of a well-designed inlet scroll becomes clear. Theoretically, such contouring produces a small loss in augmentation, but in fact, the thrust on a contoured shroud comes closer to the ideal since separation is prevented.

The more reasonable ejector in Fig. 2 consists of a contoured inlet section, a constant area mixing duct, and a diffuser. The mixing duct serves as an elongated actuator disk. Since the primary mass is only a fraction of the total flow, the deceleration of the primary jet as it mixes with the secondary stream causes the static (and total) pressure in the duct to rise. As a result of the higher static pressure in the diffuser section, the inlet-diffuser venturi develops a net lip thrust.

It is not necessary to integrate the pressure distribution on the shroud walls to determine the level of augmentation, however. The total thrust of the system can be computed by applying the conservation of mass and momentum equations to a control volume that coincides with the boundaries of the mixing duct. It will be assumed that the pressure is constant in each plane perpendicular to the direction of flow. Since the total pressure in each stream tube is equal to the atmospheric pressure, this means that the velocity of the entrained flow must be uniform and parallel to the ejector axis. The primary jet is unchoked and initially has a "top hat" velocity profile. For a well-designed nozzle and inlet, these are not unreasonable restrictions.

The shape of the velocity profile at the end of the duct will be kept arbitrary since it depends on the mixing rate of the particular jet under study. The profile function which reflects the degree to which mixing has been completed in a given augmentor may be derived from an eddy viscosity descriptive of the jet or it may be specified directly. This is the method of closure. The augmentation is computed from the profile.

The momentum balance across the mixing duct is thus written in the following form

$$P_1A_2 + \rho V_1^2A_1 + \rho V_0^2A_0 = P_2A_2 + \int_{A_2} \rho V_2^2 dA_2 \qquad (1)$$

The injection plane is such that $A_0 + A_1 = A_2$. It has been assumed that the wall shear stresses are negligible. For many ejectors this may not be true; however, in the present work, only the losses associated with the primary injection and mixing mechanisms are of interest. Flow separation, skin friction, and other such real fluid effects will be neglected. For a discussion of these losses, see Quinn.[5]

Simple algebraic manipulation of the momentum equation for incompressible flow yields

$$\int (V_2/V_0)^2\, dA_2 = [(V_1/V_0)^2\ (1 - (A_0/A_2)) + A_0/A_2 - (P_2 - P_1)/\rho V_0^2]\, A_2 \qquad (2)$$

As a measure of the flatness of the velocity profile a mixing parameter

$$\beta \equiv \int V_2^2 dA_2/\langle V_2\rangle^2 A_2 \qquad (3)$$

will be defined in terms of the mixing duct average exit velocity

$$\langle V_2\rangle = \int V_2 dA_2/A_2 \qquad (4)$$

When the flow is completely mixed, the velocity profile is flat and β is equal to unity. If the profile has any curvature, β has a value greater than one. For the unmixed flow at the inlet of an ejector, $\beta \sim 1.8$. In a well-designed ejector, the velocity at the exit of the mixing duct is almost uniform and $\beta \sim 1.02$. This parameter is used as a convenient means to express the shape of the velocity profile.

The pressure on each face of the control volume can be obtained by using Bernoulli's equation for the flows through the inlet and diffuser. Since the total pressure rises as mixing occurs, the equation does not hold across the duct. If there is no change in total pressure through the inlet or diffuser, we have for the inlet

$$P_1 = P_a - \rho V_1^2/2 \qquad (5)$$

and for the diffuser

$$P_2 = P_a - \rho\langle V_2\rangle^2/2\beta[1 - (A_2/A_3)^2] \qquad (6)$$

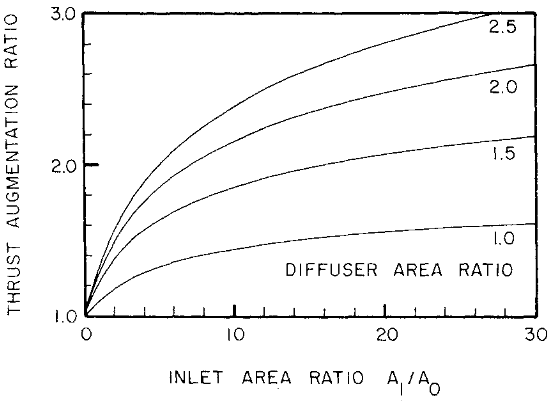

Fig. 3 Ideal levels of thrust augmentation.

The continuity equation for the flow through the diffuser has been used to eliminate $\langle V_3 \rangle$. Also, it has been assumed that the flow is diffused without further change in the value of β; that is, the velocity profile is self-preserving through the diffuser. Actually, the value of β is slightly increased by diffusion, but this is a separate area of research. Real diffuser effects will be neglected in the present analysis.

Incorporating these relations into the momentum equation yields

$$[(<V_2>/V_0)^2/2][1 + (A_2/A_3)^2] - [(V_1/V_0)^2 (1/2 - A_0/A_2) + A_0/A_2]/\beta = 0 \quad (7)$$

The continuity equation for the mixing duct is

$$<V_2>/V_0 = V_1/V_0\,[1 - (A_0/A_2)] + A_0/A_2 \quad (8)$$

The simultaneous solution of these two equations is sufficient to determine the velocity ratios at both the mixing duct inlet (V_1/V_0) and exit ($\langle V_2 \rangle/V_0$). Thus, the quantity of entrained fluid is uniquely determined by the geometry of the ejector and the value of the mixing parameter β. In an ejector of fixed length, increasing the rate of mixing results in higher levels of augmentation, since the velocity profile of a more fully mixed flow yields a value of β closer to unity. Alternately, a higher mixing rate means that a given level of augmentation (given value of β) can be achieved with a shorter mixing duct.

Since the rate at which the primary jet spreads is not a function of its initial velocity, the level of augmentation is independent of the nozzle pressure ratio. The absolute value of thrust does, of course, increase with the primary supply pressure. If η denotes the efficiency of the nozzle, the initial kinetic energy of the jet is

$$\rho V_0^2/2 = \eta[P_t - P_1] \quad (9)$$

in which P_t is the total pressure of the jet. Substituting for P_1 from the Bernoulli equation through the ejector inlet gives

$$\rho V_0^2/2 = \eta[P_t - P_a] + \eta\rho V_1^2/2 \quad (10)$$

Algebraic manipulation then yields an expression for the initial kinetic energy of the primary flow as a function of the difference between the total pressure of the jet and atmospheric pressure.

$$\rho V_0^2/2 = \eta(P_t - P_a)/[1 - \eta(V_1/V_0)^2] \quad (11)$$

This differential places an upper limit on the performance of the ejector. The function is completely determined by the geometry of the ejector and the degree of mixing [through V_1/V_0; see Eqs. (7) and (8)]. It should be noted that the primary jet velocity is increased somewhat because the static pressure in the coflowing stream of entrained fluid is less than atmospheric pressure. This produces a direct increase in the nozzle thrust, which makes a small, but not insignificant, contribution to the thrust augmentation.

Additional power is required to drive this larger mass flow through the nozzle. Since the power increment depends on the shroud geometry, ejector systems are best compared in terms of the energy input. The thrust augmentation is therefore defined as the ratio of the static thrust of the ejector to the isentropic thrust obtained by expanding the same mass of primary fluid to atmospheric pressure

$$\phi = \beta \dot{m}_3 <V_3>/\dot{m}_0 V_* \quad (12)$$

The reference jet thus has the same power requirements as the primary jet of the augmentor. Using the requirement of continuity through the diffuser the augmentation ratio can be written

$$\phi = \beta(V_0/V_*)(<V_2>/V_0)^2(A_2/A_0)(A_2/A_3) \quad (13)$$

The dependence of V_0 on the nozzle efficiency and inlet velocity ratio has been shown. $\langle V_2 \rangle/V_0$ is a known function of the inlet velocity ratio also [Eq. (8)]. Substitution yields

$$\phi = \beta\eta^{1/2}(A_0/A_3)[1 + (A_1/A_0)(V_1/V_0)]^2[1 - \eta(V_1/V_0)^2]^{-1/2} \quad (14)$$

This expression provides a convenient means to evaluate the performance of an experimental ejector. Since the level of entrainment depends upon the over-all efficiency of the ejector, a simple measurement of the inlet velocity ratio V_1/V_0 is sufficient to compute the augmentation. In theory this can be done with a single static pressure probe in the injection plane. The accuracy of the method depends, in practice, on the uniformity of the inlet flow.

Eliminating $\langle V_2 \rangle/V_0$ between Eqs. (7) and (8) produces a quadratic for V_1/V_0 as a function of the ejector area ratios and the mixing parameter β. This function can then be used with Eq. (14) as a means to predict ejector performance. In Fig. 3, the ideal augmentation has been shown as a function of the ejector configuration. It is assumed that there are no injection losses ($\eta = 1.0$) and that the flow at the exit is fully mixed ($\beta = 1.0$). It can be seen that the augmentation is significantly increased by diffusion of the mixed flow.

Energy Considerations

Even for the idealized ejector in which skin friction and flow separation are absent, there is a loss in fluid mechanical energy. Since momentum is conserved in every collision, energy must be dissipated during the transfer of momentum from the primary jet to the entrained fluid. This impact, or mixing loss occurs in the production of heat and turbulence as the two streams mix.

Far downstream of the ejector, viscosity will eventually convert the turbulent energy to thermal energy. However, for purposes of analysis it will be assumed that all the random energy appears as thermal energy of the efflux. The loss can then be computed through the equation for the conservation of energy in the mixing duct

$$\dot{m}_0\left(\frac{V_0^2}{2} + c_p T_1\right) + \dot{m}_1\left(\frac{V_1^2}{2} + c_p T_1\right) = \dot{m}_2\left(\beta\frac{<V_2>^2}{2} + c_p T_2\right) \quad (15)$$

For simplicity it has been assumed that the incoming flows are at the same temperature. A hotter primary jet would have a lower density but larger viscosity, and so the net effect on augmentation is uncertain.

Applying the requirement of continuity, $\dot{m}_2 = \dot{m}_1 + \dot{m}_0$, and the assumption that the working fluid is a perfect gas, yields

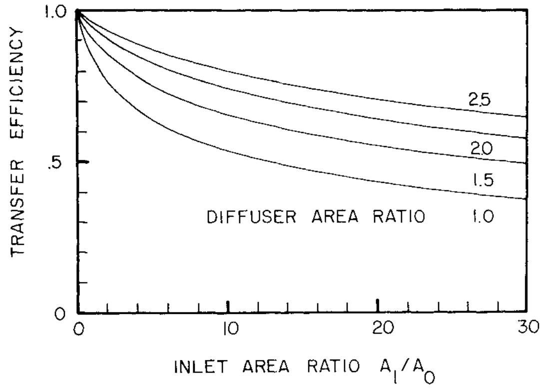

Fig. 4 Transfer efficiency of the basic fluid mixing process.

$$\dot{m}_2 c_V \Delta T = \dot{m}_0\left(\frac{P_1}{\rho_1} + \frac{V_0^2}{2}\right) + \dot{m}_1\left(\frac{P_1}{\rho_1} + \frac{V_1^2}{2}\right) - \dot{m}_2\left(\frac{P_2}{\rho_2} + \beta\,\frac{\langle V_2\rangle^2}{2}\right) \quad (16)$$

The terms on the right-hand side of this energy balance express the difference between the mechanical energies of the incoming and exiting flows. If all the lost energy goes directly into the generation of heat, the temperature of the efflux would be raised $\Delta T = T_2 - T_1$. In the present case, ΔT is on the order of 2 or 3°F. However, since an undetermined fraction of the energy loss initially appears as turbulence, T_2 becomes the effective temperature of the random energy, and the actual temperature rise is even smaller. It will, therefore, be assumed that the density remains constant.

The static pressure on each face of the control volume has been determined through Bernouilli's equation. Substitution of the relations appropriate for P_1 and P_2 reduces the expression for the energy loss to

$$\dot{m}_2 c_V \Delta T = (V_0^2/2)[\dot{m}_0(1 - V_1^2/V_0^2) - \dot{m}_2\beta(\langle V_2\rangle/V_0)^2(A_2/A_3)^2] \quad (17)$$

Even if the nozzle flow is perfectly isentropic ($\eta = 1$), there is an impact loss that depends only upon the degree of mixing obtained in the ejector. For a given value of β, this loss is a unique fraction of the initial energy, determined by the ejector area ratio and not the details of the mixing process. This means that whether a given exit velocity profile is achieved in a long mixing duct through molecular viscosity or in a shorter distance with a hypermixing jet, the impact loss is the same. Of course, there are other losses involved in a real ejector. A long mixing duct can result in large skin friction losses, and it will be seen that there is a loss in thrust efficiency for the hypermixing nozzles. The over-all ejector efficiency is determined by the combination of all such losses.

The efficiency of the energy transfer process is defined to be the ratio of the kinetic energy of the efflux

$$\dot{m}_0\beta\,\frac{\langle V_2\rangle^2}{2}\left(\frac{A_2}{A_3}\right)^2 \quad (18)$$

to the energy input,

$$\dot{m}_0\beta(P_t - P_a)/\rho \quad (19)$$

In the one-dimensional ejector under consideration, the transfer efficiency is

$$n = \eta\beta\left[\frac{A_2}{A_3}\right]^2\left[1 + \frac{V_1}{V_0}\frac{A_1}{A_0}\right]^3\left[1 + \frac{A_1}{A_0}\right]^{-2}\left[1 - \eta\left(\frac{V_1}{V_0}\right)^2\right]^{-1} \quad (20)$$

The fraction of the initial energy that remains when mixing is complete, if there are no other losses ($\eta = \beta = 1.0$) is shown in Fig. 4. It can be seen that the increase in augmentation with inlet area ratio is accompanied by a decrease in transfer efficiency. Although diffusion of the flow improves this efficiency somewhat, the relatively large mixing loss associated with high levels of augmentation means that the ejector cannot be competitive with the rotor in extended hover. However, the augmentor performs better during transition and cruise and offers such clear advantages in mechanical simplicity and noise reduction that it remains attractive for VSTOL applications.

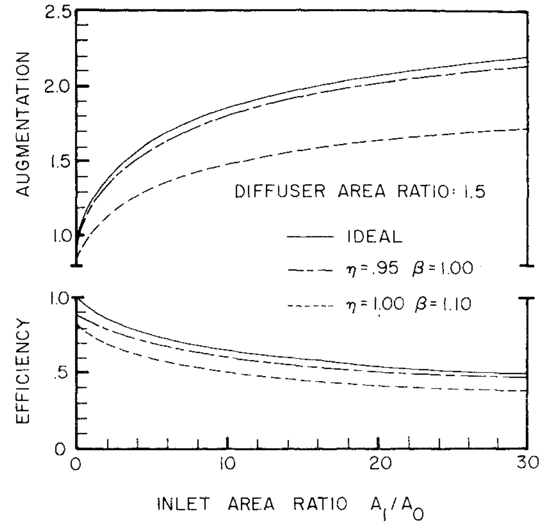

Fig. 5 Effect of the primary loss mechanisms.

Secondary/Primary Mixing

For an ejector of given area, the augmentation is a function of the degree to which mixing has been completed. At the rate of ordinary turbulent entrainment, this means that a long mixing section is required to achieve practical levels of augmentation. The performance of an ejector shortened to meet aircraft size and weight limitations therefore must be less than optimum.

Figure 5 illustrates the loss in augmentation when mixing is imcomplete. Even for the almost fully mixed flow described by a value of $\beta = 1.1$, the augmenting thrust is reduced by approximately 40%. It might be expected that because the mixing is incomplete, less energy is lost in the production of turbulence. However, for the case $\beta = 1.1$ an additional 5-10% loss in the transfer efficiency can be seen in Fig. 5. This decrease is generated because the turbulence stresses are proportional to the difference in the velocity of the jet and that of the coflowing stream of entrained fluid. When mixing is complete, a larger volume of fluid is entrained through the inlet per unit time, minimizing this difference.

A loss in nozzle thrust efficiency, on the other hand, does not produce so large a penalty in augmentation. The approach taken at ARL is that an improvement in the performance of short ejectors can be obtained by increasing the rate of entrainment, even at some cost in nozzle efficiency. This is also shown in Fig. 5 for an augmentor in which the flow is fully mixed, but the nozzle efficiency, $\eta = 0.95$, is less than isentropic.

It is reasonable to suppose that the rate at which a turbulent jet spreads depends upon the scale and intensity of the turbulence. Introducing streamwise vorticity into the jet discharge amounts to redistributing the energy of the flow by controlling the scale of the turbulence. This is accomplished in the hypermixing nozzles utilized in the present work by segmenting the exit plane of a slot nozzle,

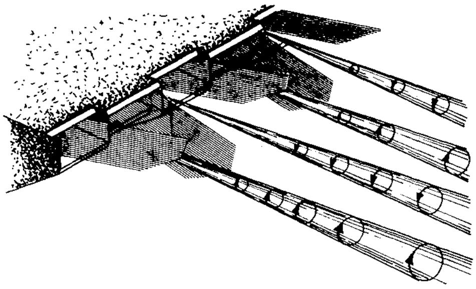

Fig. 6 Alternating exit of the hypermixing nozzle.

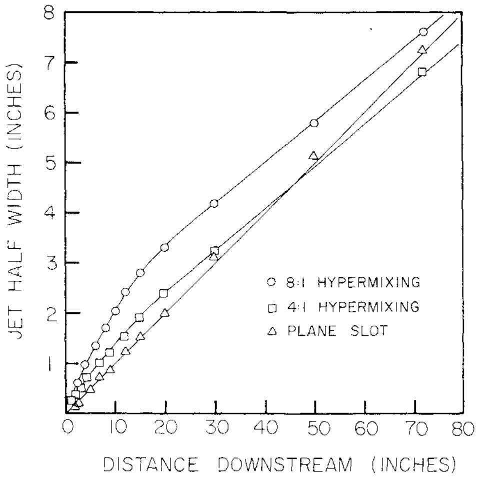

Fig. 7 Comparison of jet spreading rates.

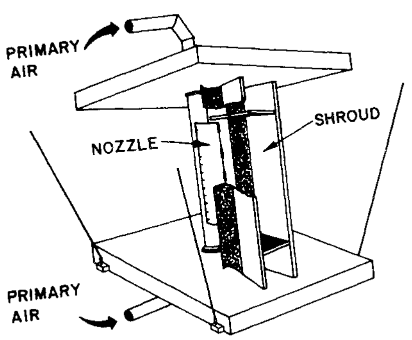

Fig. 8 Sketch of the experimental apparatus.

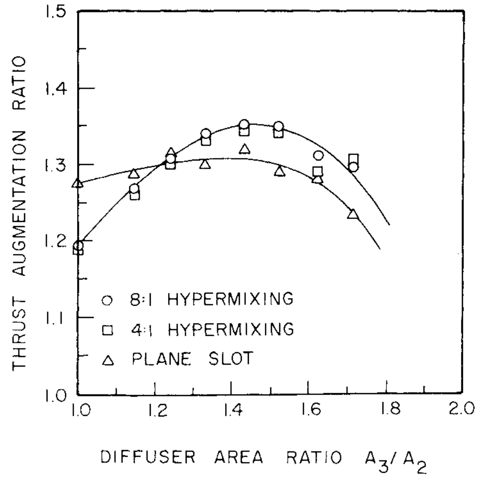

Fig. 9 Measured augmentation of the inlet area ratio 6.5 ejector.

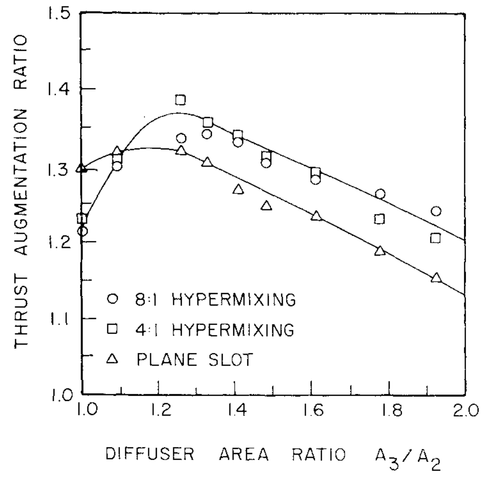

Fig. 10 Measured augmentation of the inlet area ratio 8.6 ejector.

as shown in Fig. 6. The flow on the shorter side of each element expands to atmospheric pressure more rapidly. The resultant local pressure difference deflects the segment of the jet to the short side, as shown. By alternating the surfaces which are cut back, the desired vorticity is produced. These vortices serve to entrain additional fluid and accelerate mixing throughout the jet.

A measure of the spreading rate is the jet half width: half the width of the jet at the point where the velocity is half the peak velocity. The half width of two hypermixing nozzles has been compared to that of an equivalent area slot nozzle in Fig. 7. The hypermixing nozzles have the same slot thickness; they differ in the aspect ratio of the individual segments. The aspect ratio 4:1 nozzle is divided into 24 elements, each 1½ in. long. The 8:1 nozzle has half as many segments, but each is 3 in. long. It is believed that the lesser growth rate of the 4:1 nozzle is caused by destructive interference between the vortices. The slot nozzle has the same 36 in. length and 0.375 in. width as the hypermixing nozzles.

The cross section of any finite aspect ratio slot jet asymptotically approaches the circular. An analytic treatment of this phenomenon has been developed by Viets.[6] All three nozzles were therefore tested with sidewalls in order to limit entrainment at the ends of the major axis of the jet sheet and insure that the spreading rate was approximately two-dimensional. For the first hundred widths downstream the growth of both hypermixing jets is greater than that of the equivalent plane turbulent jet.

Experimental Apparatus

The experimental ejector is supported by four cables as a kind of swing. A sketch of the apparatus is seen in Fig. 8. The absolute thrust is measured with a fixed load cell which bears against the forward surface of the lower platform. The cell is preloaded so that no motion of the swing actually occurs. Air is supplied to the primary manifold from a single source through upper and lower feed pipes. The rate of primary mass flow is measured with a calibrated orifice plate upstream of the dual feed. Primary stagnation conditions are measured with a kiel probe and thermocouple in the manifold.

Since the ejector shroud can be removed, this facility was also used to measure the efficiency of each nozzle. An integrating rake was employed to determine the spreading rate of the freejets presented in Fig. 7. Other details relevant to the test apparatus and instrumentation have been reported in Refs. 3 and 5.

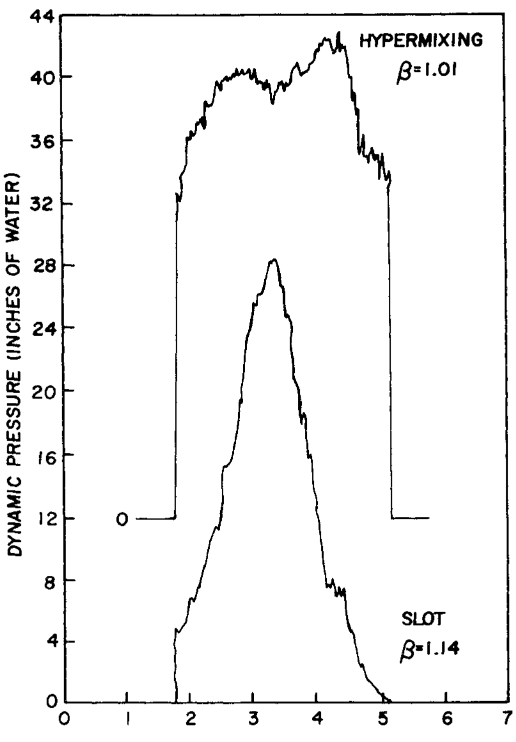

Fig. 11 Dynamic pressure profiles at the diffuser inlet.

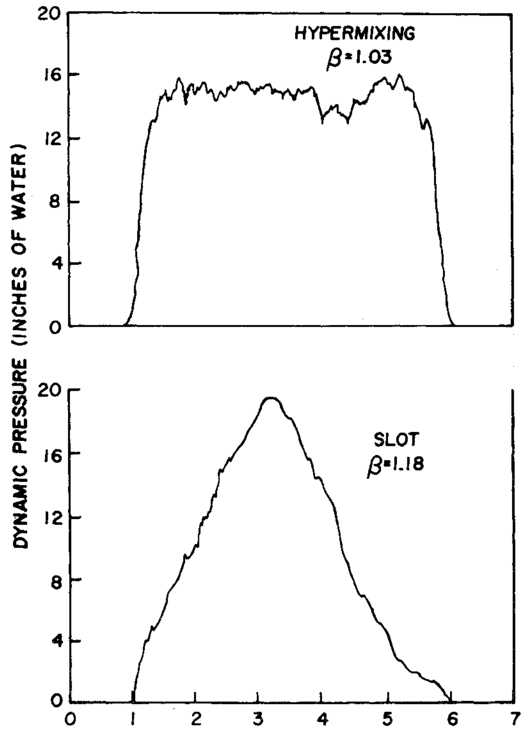

Fig. 12 Dynamic pressure profiles at the diffuser exhaust.

The accuracy of the system was established by testing over a range of pressure ratios with an axisymmetric calibration nozzle whose area was equal to that of the slot nozzle. A linear regression analysis of these data points revealed the 90% confidence band to be within ±2% of the mean.

Results and Discussion

The thrust efficiency of the reference slot nozzle was measured to be $\eta = 0.97$. The efficiencies of the two hypermixing nozzles were found to be essentially the same, $\eta = 0.95$. The unconventional form of the hypermixing exit makes a determination of the exit area somewhat arbitrary. However, the effective area can be computed from the measured rate of mass flow and the flow per unit area calculated for an isentropic expansion to atmospheric pressure from the stagnation state. This computation gave $A_0 = 12.6$ in.^{2} for both nozzles.

The ejector shroud consists of a contoured inlet scroll 2 in. deep, a constant area mixing duct 12 in. long, and a 7 in. diffuser. Tests were conducted at inlet area ratios A_1/A_0 of 6.5 and 8.6 over a range of diffuser area ratios. Due to the use of an improved inlet contour, there is an overall increase in the augmentation above that reported in Ref. 3. However, no means of boundary-layer control was included in the shroud, so that there was separation from the diffuser end walls. The assembly is considered to be a nozzle evaluation facility and, as such, no further attempt was made to obtain maximum performance.

The measured levels of thrust augmentation are compared at each area ratio in Figs. 9 and 10. The lines drawn through the data are not the product of analysis, but are intended to aid the eye in linking individual points. It can be seen that at low diffuser area ratios, the slot nozzle produces larger values of augmentation than either of the hypermixing nozzles. This is because it has a greater efficiency and, since the more slowly spreading jet impinges on the wall further downstream, less friction is generated in the duct.

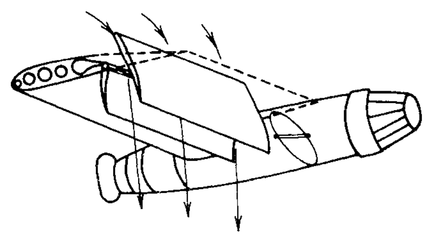

Fig. 13 Sketch of a turbofan-powered augmentor flap.

However, as the exit area is increased, the diffuser in the ejector with the slot nozzle stalls, and there is a resultant loss in augmentation. A representative dynamic pressure profile at the diffuser inlet for this case is shown in Fig. 11. This profile was measured in the inlet area ratio 8.6 ejector at a diffuser area ratio of 1.48. The profile produced by the 8:1 hypermixing nozzle under these same conditions is also shown. In Fig. 12, the corresponding profiles of the dynamic pressure at the diffuser exit are shown for comparison. It is significant that the value of β is increased by diffusion and that the increase is greatest for the case of the slot nozzle. This is not a new result (it was mentioned by Prandtl[7] in 1933, for example), but it means that diffusion of an unmixed flow results in less than the theoretical pressure recovery, even without diffuser stall. The augmentation continues to increase with the diffuser area ratio in the hypermixing configurations. Both hypermixing nozzles in fact produce higher maximum augmentation than the slot nozzle.

The effect of change in nozzle position on performance was investigated in other tests. A further increase in thrust (4% of the total thrust, 18% of the augmenting thrust) was obtained by changing the injection plane to the leading edge of the inlet scroll. The additional 2 in. of length thus provided results in a flatter profile at the diffuser inlet, and accounts for most of the gain. A maximum 5% loss in total thrust was seen over a range of shroud angles relative to the primary thrust vector of from

0° to 60°. The small variations in performance produced by these changes suggests that an ejector with a similar configuration offers good possibilities for use as an augmentor flap on the trailing edge of a conventional wing. Such an application is illustrated in Fig. 13.

Conclusions

Analysis has shown that the thrust augmentation of compact aircraft ejectors can be increased by accelerating the rate of entrainment by the primary jet. This has been accomplished with a nozzle which produces a series of streamwise vortices in the jet discharge. Tests of a simple ejector employing these nozzles have shown a significant improvement in the level of augmentation.

The effect of hypermixing is not to improve ejector performance by minimizing losses. In fact, the lower efficiency of hypermixing nozzles is a penalty that must be paid for their use. However, by assuring more nearly complete mixing of the flow in a shorter distance, it becomes possible to diffuse the flow efficiently and consequently obtain higher net levels of augmentation.

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Integrated Airframe-Nozzle Performance for Designing Twin-Engine Fighters

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Performance data for designing fighter aircraft having twin buried engines and dual nozzles were obtained from an experimental investigation of over 200 large-scale, twin-nozzle/aftbody configurations. Sufficient pressure and force balance data were obtained for exhaust nozzle pressure ratios within the operating range of a typical advanced technology engine to allow the effect of the following aft-end design variables to be determined: nozzle type, power setting position, axial position, solid body exhaust simulation, and lateral spacing; interfairing type, length, height, and base area; vertical stabilizer type, position, and rudder deflection; horizontal stabilizer deflection; and fuselage area distribution. In order to utilize the wind-tunnel data for predicting aircraft performance, the effects on aft-end drag of support system interference, inlet mass flow, lifting surface span reduction, and tunnel Reynolds number were also determined.

Nomenclature

A_{CC} = nozzle cross-sectional area at *F.S.* 133.182, customer connect station
A_E = nozzle exit area
A_{MB} = aftbody boattail cross-sectional area at *F.S.* 113.188, metric break area
A_P = stabilizer planar area
A_S = nozzle shroud internal cross-sectional area at nozzle exit station
A_T = nozzle throat area
A_W = wing reference area, 2667.6 in.^{2}
A/B = afterburning power setting
$C_{D(AE)}$ = aft-end drag coefficient—includes nozzle and aftbody fuselage and interfairing drag—based on nozzle axes and wing area, A_W
C_P = pressure coefficient
D_{CC} = maximum exposed nozzle diameter, 8 in.
F.S. = fuselage station, longitudinal distance aft of a point 1.041 in. aft of model nose
L = distance from customer connect station to trailing edge of interfairing or nozzle, inches
M_∞ = freestream Mach number
NPR = nozzle pressure ratio (nozzle total/ambient static)
Re = Reynolds number per foot
S/D = nozzle lateral spacing ratio, distance between nozzle centerlines divided by D_{CC}
X = distance
α = interfairing upper surface trailing edge angle relative to nozzle centerline, degrees
β = interfairing lower surface trailing edge angle relative to nozzle centerline, degrees
$\Delta C_{D(AE)}$ = aft-end drag coefficient increment due to strut interference effects

Introduction

THE achievement of proper airframe/nozzle integration has become significantly more difficult and important with the advent of the multimission aircraft requiring variable geometry nozzles to operate over a broad range of altitudes and Mach numbers. The mutual interactions that occur between the nozzle exhaust and the external flowfield can alter the pressure distributions on the aft-end of the fuselage and produce both internal and exter-

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