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PERFORMANCE COMPARISON OF AERO-ENGINE THRUST AUGMENTORS STABILIZED BY BLUFF BODY FLAME HOLDERS AND A FLAME-HOLDER-LESS CONCEPT

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ABSTRACT

This study investigates the performance benefits of a flame-holder-less flame stabilization concept for thrust augmentors compared to the common flame holder design. The concept proposes to burn a small portion of the augmentor fuel in a rich mixture with air bled from the compressor to produce a highly reactive partially oxidized fuel-air mixture (POx). The POx mixture is injected into the turbine exit flow to enhance combustion kinetics in order to achieve stable combustion in the augmentor. Thermal efficiency during wet and dry operation is compared, taking into account both the pressure losses due to the flame holders and the reduction of core air for the flame-holder-less concept. Furthermore, the thrust-to-weight ratio and the corresponding flight range have been investigated with respect to the system weight and the induced losses.

It was found that the thermal efficiency during dry operation is significantly increased when the pressure losses of the flame holders are eliminated. During wet operation, it was calculated that a flame holder system with only 2 % total pressure loss of the flow would operate at the same thermal efficiency as the flame-holder-less concept when 3 % air is bled from the compressor. If, for an engine operating at these conditions, the flame-holder-less system could maintain stable combustion using less than 3 % bleed air, it would increase the thermal efficiency of that engine during wet operation. The results also suggest that a flame-holder-less system is lighter weight and has the potential to increase engine thrust-to-weight ratio and extend flight range when compared to a flame holder system designed to operate at the same overall engine thermal efficiency.

Nomenclature

c	Velocity
$\frac{c_L}{c_D}$	Lift-to-drag ratio
c_p	Specific heat capacity at constant pressure
g	Gravitational acceleration
m	Mass
$\dot{m}$	Mass flow rate
p	Pressure
sfc	Specific fuel consumption
t	Wall thickness
A	Area
D	Diameter
E	Material quality coefficient

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EHV	Equivalent heating value
F	Thrust
FPR	Fan pressure ratio
FR	Flight range
LHV	Lower heating value Jet A
M	Mach number
OPR	Overall pressure ratio
POx	Partially oxidized fuel-air mixture
POR	Partial oxidation reactor
PR	Power ratio
$\dot{Q}$	Heat flux
R	Specific gas constant
S	Material stress value
T	Temperature
Y	Material property
η	Efficiency
π	Pressure recovery factor
Φ	Mass based equivalence ratio
Subscript	
i	Inner (diameter)
o	Outer (diameter)
s	Isentropic
$spray$	Spray bar
t	Total/stagnation state
AB	Afterburner
By	Bypass
BC	Main burning chamber
C	Compressor
N	Nozzle
T	Turbine

I INTRODUCTION

The turbofan engine thrust augmentor is a crucial element for most military aircraft propulsion systems. Tactical aircraft in general must perform well in all areas of a large flight envelope, in terms of both altitude and speed. The thrust augmentor is crucial for these aircraft to meet requirements for takeoff, maneuvering and acceleration. Thrust augmentation is achieved through the injection of the aircraft's fuel into the core exhaust stream after it leaves the turbine section but prior to entering the exhaust nozzle. This fuel must be ignited to increase thrust. However, the high flow velocity and low pressure, relative to the engine's main combustor, of this flow make efficient combustion of this fuel a considerable challenge.

The most common method for stabilizing the combustion process in thrust augmentors has historically been with the use of bluff body flame holders, a trend that continues today. These flame holders create a recirculation zone in their wake that allows for the combustion process to anchor itself. Though engine designers have achieved great success with this method, the concept is not without its drawbacks. A more detailed review of the following disadvantages can be found in the literature [1]. First, there is an inevitable pressure loss in the core flow of the engine due to the significant area blockage. Modern augmentors typically employ flame holders which induce a 2 % – 5 % loss in total pressure [2]. The flame holders are always present as a flow obstruction, and are therefore reducing the engine's efficiency and thrust output during both "wet" (augmentor in use) and "dry" (augmentor not in use) operation. Second, rising turbine exit velocities and temperatures require a new close-coupled flame holder concept with active cooling to meet durability requirements for the fuel delivery and flame holder systems. This close-coupled concept increases the weight and complexity of the augmentor to the detriment of performance and ease of maintenance. Third, the close-coupled concept reduces the distance between the fuel injection location and the recirculation zone where the flame stabilizes. This reduction in distance leads to the fuel distribution's and fuel spray dynamics' influencing the combustion process. As a result, both static ("blowout") and dynamic ("screech") instabilities can occur unexpectedly at different augmentor operating conditions or turbine exit conditions. Fourth, these metal components are at a high temperature and are visible from the rear of the engine, which means that they can increase the radar and infrared signature of the aircraft.

In an effort to reduce or eliminate the afore-mentioned disadvantages of bluff body flame stabilization, a concept is under development at the Georgia Institute of Technology to stabilize the augmentor combustion process without the need of flame holders. The concept achieves stable combustion by combining the autoignition characteristics of the augmentor fuel with the aid of a highly reactive mixture which accelerates the fuel's chemical kinetics. As turbine exit temperatures rise [1], ignition delay times for hydrocarbon fuels such as liquid jet fuel are reduced [3]. It has been shown by Birmaher et al. that, with judicious placement of the fuel injection location and an injection method that provides adequate mixing, liquid Jet A fuel can autoignite close to the turbine exit [4]. However, it was also suggested that some form of trigger gas would be necessary to anchor the ignition location to reduce sensitivity to variations in core flow conditions. Birmaher et al. achieved this triggering effect experimentally with hydrogen gas.

Due to the impracticality of using pure hydrogen, current efforts seek to achieve stabilization using partial oxidation (POx) mixtures. POx mixtures are the product of rich hydrocarbon combustion, and include H_2 and CO as their highly-reactive species with significant amounts of N_2, CO_2 and H_2O also present. Research in the field of internal combustion engines, e.g. Eng et al. [5], has suggested that POx mixtures can promote the ignition of vaporized hydrocarbon fuels. Cutright et al. has shown that a simulated POx mixtures effectively reduce the ignition temperature of gaseous propane fuel and that, for a given vitiated air temperature, the addition of such mixtures reduce the fuel's ignition delay [6].

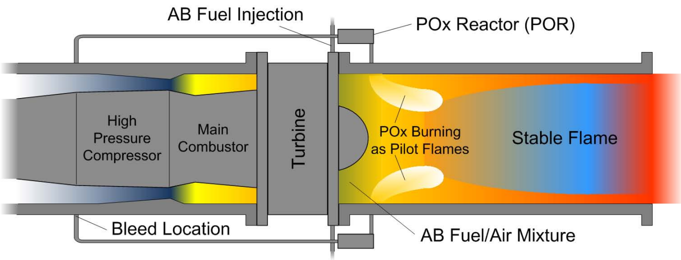

FIGURE 1: ILLUSTRATION OF A TURBOJET UTILIZING THE FLAME-HOLDER-LESS STABILIZATION CONCEPT. EXTRACTED AND MODIFIED FROM [6].

Figure 1 provides a schematic of how this proposed concept could be employed in a conventional turbojet engine. Augmentor fuel is injected close to the turbine exit where it begins to vaporize and mix with the core air. Meanwhile, a small amount of air is bled from some location along the compressor and fed to a partial oxidation reactor (POR) where it is combined with excess fuel and ignited to produce a POx mixture. The POx mixture is then injected into the augmentor flow stream where it promotes the ignition of the augmentor fuel. A more detailed description of this implementation is given in Section II.

The purpose of the flame-holder-less concept is to reduce, if not eliminate, the drawbacks of flame holders mentioned earlier in this section. However, for the new concept to be proven advantageous, it must prove both to be an effective method for stabilizing the augmentor combustion process as well as to provide comparable or even superior engine performance. Work being performed currently at the Georgia Institute of Technology serves to investigate the possibility of the former criterion. As to the latter criterion, the work presented in this document seeks to evaluate the performance of two identical engines using both concepts for flame stabilization.

To evaluate the performance of this proposed flame-holder-less concept, this paper presents the results of a thermodynamic analysis and comparison study of both the flame holder and flame-holder-less concepts on a modeled aero-engine. The evaluation compares the two concepts in terms of engine thermal efficiency during both wet and dry operation, engine weight and thrust to weight ratio, and the resulting effect on maximum flight range. The layout of the model used for the performance analysis is presented in Section III along with a description of how this model was implemented. Because it is not yet known how much air must be bled from the compressor, or from what location the air must be drawn, it became necessary to evaluate the flame-holder-less concepts performance over a range of bleed-air flow rates and bleed locations. The analysis presented here serves two purposes. First, once one can determine the parameters necessary to stabilize augmentor combustion for a given engine (i.e., bleed-air flow rates and locations for the flame-holder-less concept and flame holder pressure drop for the flame holder concept), one can directly compare the two concepts in terms of engine performance using the modeling method presented. Second, the overall results of the analysis serve to elucidate the relative impact both concepts have on engine performance.

II THE FLAME-HOLDER-LESS AUGMENTOR CONCEPT

As described above, the proposed concept uses no flame holders to provide stable combustion in the augmentor. Instead, the mixture of augmentor fuel and vitiated air ignites by autoignition. The addition of a highly-reactive POx mixture accelerates the chemical kinetics to anchor the process. To generate this POx mixture, a small amount of compressor air is re-routed to an auxiliary reactor where it is combined with excess fuel and burned. This rich combustion process produces a high-temperature mixture of, among other species, H_2 and CO [6]. If heat transfer out of the POR is minimized, most of the energy of the fuel goes either to the formation of the H_2 and CO (formation enthalpy) and raising the temperature of the mixture (sensible enthalpy). For a given fuel, the amount of energy that goes to either mode is a function of the equivalence ratio. As this POx mixture is added to the augmentor core flow, the only heating value of the fuel that is not added to the augmentor flow is what heat is lost from the POR and piping through heat transfer. For the purposes of this study, it is assumed that the POR is operating adiabatically and that the full heating value of the POR fuel is recouped in the augmentor.

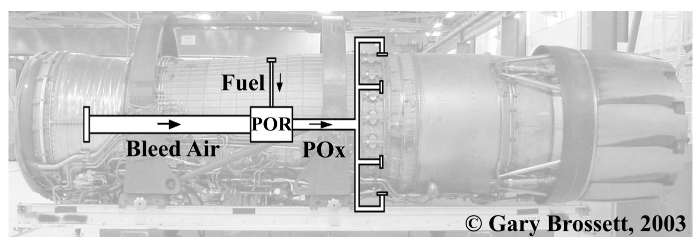

FIGURE 2: SCHEMATIC OF THE ROUGH POSITIONING OF A SIMPLIFIED FLAMEHOLDERLESS FLAME STABILIZATION SYSTEM ON THE OUTSIDE OF AN AERO-ENGINE. COURTESY OF THE AIRCRAFT ENGINE HISTORICAL SOCIETY.

A schematic of a proposed design of the flame-holder-less system and the rough positioning of its components is shown in Fig. 2. Since the POR must draw air from the compressor and then deliver it to the augmentor, a certain amount of piping must also be present. As a practical requirement, the POR and its pipe network must be both lightweight and compact. If the system is significantly heavier than the flame holders it replaces, performance improvements may be effectively limited. For the design that is depicted in Fig. 2, the system is installed outside the augmentor section casing. Hence, the system must also be compact in order to limit the increase in the engines footprint.

Feasibility

Recent work has demonstrated that a compact POR can successfully generate POx mixtures from liquid Jet A in the pressure range of 1 to 2 atm, and that these mixtures effectively increase the ability of liquid Jet A to ignite at the typical augmentor flow conditions given by Lovett et al., [1]. Figure 3 shows an image of liquid Jet A fuel being injected in co-flow with a vitiated air stream of 900°C and 150 m/s. Under these conditions, the liquid fuel would not ignite and no flame was visible. However, a POR was connected to the gas stream generator with an injection point located roughly 8 *in*. upstream of the Jet A injection. At the time this image was taken, the POR was operating at an equivalence ratio of 1.9. When the POx mixture from the POR was added to the vitiated air flow, ignition of the Jet A fuel occurred as seen in Fig. 3.

FIGURE 3: VIEW AT THE EXHAUST SECTION OF AN AFTERBURNER TEST RIG. FLOW FROM LEFT TO RIGHT. VITIATED AIR AT APPROXIMATELY 900°C AND 150 m/s IS MIXED WITH JET A. UNDER THESE CONDITIONS, IGNITION COULD ONLY BE ACHIEVED WITH THE ADDITION OF A POx MIXTURE.

Other design considerations such as compressor operability during POR operation, POR-cooling requirements, and heat transfer from the POR to other engine components also affect the feasibility of the flame-holder-less concept. Such considerations are being addressed in the ongoing work performed at the Georgia Institute of Technology, but are not within the scope of the work presented here.

System Layout

The piping of the flame-holder-less flame stabilization system has to consist of the connection between the air bleed location at the compressor and the POR, and of the distribution pipe system from the POR to the multiple injection locations in the augmentor. For this comparison study, a single POR is assumed which is lined up with the main combustor, and both insulation and fuel feed piping have been neglected.

Figure 4 shows the dimensions of the POR and pipe network that were assumed for this study. These dimensions were estimated based on an engine of the 60 kN thrust class with up to 80 kN of thrust during wet operation. Such an engine requires an air mass flow rate and an inlet diameter of approximately 80 kg/s and 0.75 m, respectively. Bleed air pipe length varies depending on the locations along the compressor.

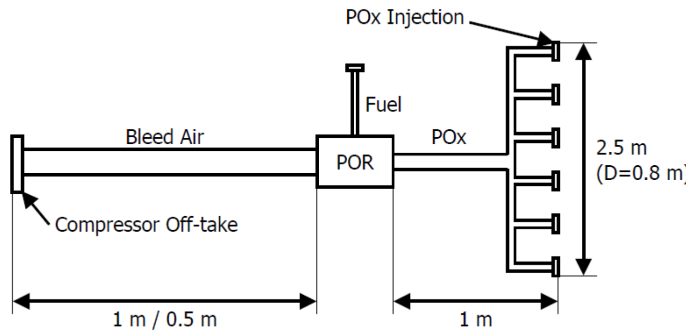

FIGURE 4: SIMPLIFIED FLAME-HOLDER-LESS SYSTEM WITH DIMENSIONS MATCHING TO AN ENGINE WITH A MASS FLOW OF APPROXIMATELY 80 kg/s AND 4 m LENGTH. THE VALUES FOR THE PIPE AT THE LEFT-HAND SIDE REFER TO DIFFERENT BLEED POSITIONS ALONG THE COMPRESSOR.

III CYCLE ANALYSIS

The schematic of the engine modeled in this study is shown in Fig. 5. The numbered stations correspond to the station numbers used in the model. The cycle is described by the following process.

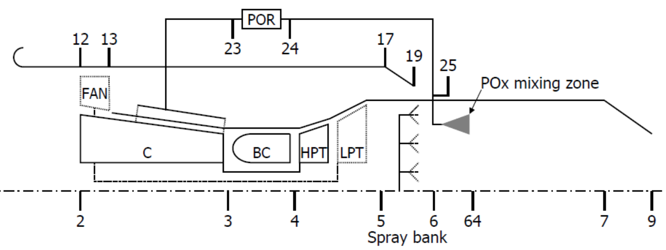

FIGURE 5: STATION DEFINITION USED IN THE CYCLE ANALYSIS CODE. DOTTED LINES AT THE COMPRESSOR INDICATE DIFFERENT BLEED LOCATIONS.

The air flow at station 0, $\dot{m}_0$, is at ambient conditions and at flight Mach number M and undergoes an ideal ramming deceleration process. If the diffuser is ideal, the inlet pressure p to the compressor at station 2 is

$$p_{t,2} = p_0 \cdot \left(1 + \frac{\gamma_2 - 1}{2} \cdot M_2^2\right)^{\frac{\gamma_2}{\gamma_2 - 1}}, \tag{1}$$

where γ represents the ratio of specific heats.

The air is further compressed between stations 2 and 3:

$$p_{t,3} = p_{t,2} \cdot OPR, \tag{2}$$

where OPR stands for the compressor overall pressure ratio. With the isentropic compressor efficiency $\eta_{C,s}$, the temperature

T is given by

$$T_{t,3} = T_{t,2} \cdot \left(1 + \frac{OPR^{\frac{\gamma_{2,3}-1}{\gamma_{2,3}}} - 1}{\eta_{C,s}} \right). \tag{3}$$

The same considerations apply for the compression in the bypass from station 12 to 13 with the fan pressure ratio FPR and the isentropic fan efficiency $\eta_{Fan,s}$.

Through the main combustor from stations 3 to 4, the pressure is reduced, which is expressed by a pressure recovery factor π:

$$p_{t,4} = p_{t,3} \cdot \pi_{BC}, \tag{4}$$

while raising the temperature to the design turbine inlet temperature requires a fuel mass flow rate $\dot{m}_{fuel,BC}$ of

$$\dot{m}_{fuel,BC} = \frac{\dot{m}_4 \cdot c_{p,4} \cdot T_{t,4} - \dot{m}_3 \cdot c_{p,3} \cdot T_{t,3}}{\eta_{BC} \cdot LHV}, \tag{5}$$

where c_p and LHV represent a specific heat capacity at constant pressure and a lower heating value of the fuel, respectively. The fuel chosen for this engine was Jet A.

Work extraction and expansion in the turbine decrease the pressure from station 4 to 5, hence

$$p_{t,5} = p_{t,4} \cdot \left(1 + \frac{\frac{T_{t,5}}{T_{t,4}} - 1}{\eta_{T,s}} \right)^{\frac{c_{p,4,5}}{R_4}}, \tag{6}$$

while the temperature drop is determined by the balance of power with the compressor:

$$\begin{aligned} T_{t,5} =& \frac{\dot{m}_4 \cdot c_{p,4} \cdot T_{t,4} - (\dot{m}_3 \cdot c_{p,3} \cdot T_{t,3} - \dot{m}_2 \cdot c_{p,2} \cdot T_{t,2})}{\dot{m}_5 \cdot c_{p,5}} \\ &- \frac{(\dot{m}_{13} \cdot c_{p,13} \cdot T_{t,13} - \dot{m}_{12} \cdot c_{p,12} \cdot T_{t,12})}{\dot{m}_5 \cdot c_{p,5}} \end{aligned} \tag{7}$$

Between stations 5 and 6, the blockage due to the spray bars reduces the pressure, leaving the temperature constant:

$$p_{t,6} = p_{t,5} \cdot \pi_{spray}. \tag{8}$$

The pressure of the bleed air at station 23, which depends on the bleed location and OPR, is reduced in the POR to

$$p_{t,24} = p_{t,23} \cdot \pi_{POR}. \tag{9}$$

The total pressure recovery, π_{POR}, was assumed to be unity for the analysis performed here. It was also assumed for this analysis that no total pressure losses occured in the piping from the compressor off-take position to station 23 and from stations 24 to 25.

From stations 23 to 24, the temperature rise for rich combustion in the POR is modeled using CHEMKIN by a perfectly-stirred reactor burning propane. The combustion process itself is modeled using the GRI Mechanism 3.0. Propane was chosen to model the augmentor fuel in the POR due to its reasonable agreement with the properties of gaseous Jet A and because few defined mechanisms successfully model the combustion of Jet A.

The POx temperature is predicted from $T_{t,25} = T_{t,24} + \Delta T_t$ with

$$\begin{aligned} \Delta T_t(p_{t,23}, \Phi_{POR}) = 2926.00 &- 1.46 \cdot \Phi_{POR} \cdot p_{t,23} \\ &- 1081.00 \cdot \Phi_{POR} + 150.10 \cdot \Phi_{POR}^2 \\ &- 4.12 \cdot p_{t,23} + 0.11 \cdot p_{t,23}^2, \end{aligned} \tag{10}$$

where Φ_{POR} represents a POR equivalence ratio.

The energy Q stored in the reaction products is given by

$$\dot{Q}_{POx} = \dot{m}_{fuel,POR} \cdot (LHV - EHV), \tag{11}$$

with an equivalent heating value EHV of

$$EHV = \frac{\dot{m}_{24} \cdot c_{p,24} \cdot T_{t,24} - \dot{m}_{23} \cdot c_{p,23} \cdot T_{t,23}}{\eta_{POR} \cdot \dot{m}_{fuel,POR}}. \tag{12}$$

To model POx mixture injection, the mixture is accelerated from station 24 to 25 to balance the static pressures, $p_6 = p_{25}$. The POx is mixed into the core flow through coaxial injection in a constant area duct, $A_{64} = A_6$, where the pressure at station 64 is elevated. Mass, momentum and energy equation have to be solved, which is given by

$$\begin{aligned} \frac{p_{64}}{R_{64} \cdot T_{64}} \cdot A_{64} \cdot c_{64} =& \frac{p_6}{R_6 \cdot T_6} \cdot A_6 \cdot c_6 + \frac{p_{25}}{R_{25} \cdot T_{25}} \cdot A_{25} \cdot c_{25}, \\ \frac{p_{64}}{R_{64} \cdot T_{64}} \cdot A_{64} \cdot c_{64}^2 =& \frac{p_6}{R_6 \cdot T_6} \cdot A_6 \cdot c_6^2 + \frac{p_{25}}{R_{25} \cdot T_{25}} \cdot A_{25} \cdot c_{25}^2 \\ &+ (p_6 - p_{64}) \cdot A_{64}, \\ \dot{m}_{64} \cdot c_{p,64} \cdot T_{t,64} =& \dot{m}_6 \cdot c_{p,6} \cdot T_{t,6} + \dot{m}_{25} \cdot c_{p,25} \cdot T_{t,25}, \end{aligned} \tag{13}$$

where c represents velocity. Because POx injection was coaxial, accounting for the axial momentum balance incorporated the mixing losses.

In the augmentor and the nozzle from station 64 through station 7 then 9, the pressure is further decreased and is expressed through recovery factors:

$$p_{t,9} = p_{t,7} \cdot \pi_{AB} \cdot \pi_N. \tag{14}$$

The pressure ratio $\frac{p_{t,9}}{p_0}$ determines whether the convergent nozzle is choked, or not. If the nozzle is choked, then the pressure ratio determines the exit velocity for expansion to ambient pressure $c_{9,id} \neq c_9$, which is required for the thermal efficiency calculations. The same considerations apply for the bypass nozzle, station 19.

The required augmentor fuel mass flow rate to reach the design temperature is given by

$$\dot{m}_{fuel,AB} = \frac{1}{\eta_{AB} \cdot LHV} \left(\dot{m}_7 \cdot c_{p,7} \cdot T_{t,7} - \dot{m}_{64} \cdot c_{p,64} \cdot T_{t,64} - \dot{Q}_{POx} \right). \tag{15}$$

The total core flow equivalence ratio can now be defined as

$$\Phi_{Total} = \frac{\dot{m}_{fuel,BC} + \dot{m}_{fuel,AB} + \dot{m}_{fuel,POR}}{\dot{m}_2} \cdot \left(\frac{\dot{m}_{air}}{\dot{m}_{fuel}} \right)_{stoich}, \tag{16}$$

where $\dot{m}_2$ is the mass flow rate of core air before any compressor air is bled. Care was taken to maintain a constant Φ_{Total} to ensure that the performance analysis of the system was not affected by changes in total energy added from the fuel. Variation of this value over the full parameter space used in this model was less than 0.5 % of full scale.

From the $\dot{m}_{fuel,AB}$ value, the bleed air mass flow rate $\dot{m}_{23}$ and the POR fuel mass flow rate $\dot{m}_{fuel,POR}$ are calculated through the power ratio $PR = \frac{\dot{m}_{fuel,POR}}{\dot{m}_{fuel,AB}}$ and the POR equivalence ratio

$$\Phi_{POR} = \frac{\dot{m}_{fuel,POR}}{\dot{m}_{23}} \cdot \left(\frac{\dot{m}_{air}}{\dot{m}_{fuel}} \right)_{stoich}, \tag{17}$$

hence

$$\dot{m}_{23} = \frac{\dot{m}_{fuel,POR}}{\Phi_{POR}} \cdot \left(\frac{\dot{m}_{air}}{\dot{m}_{fuel}} \right)_{stoich} \tag{18}$$

with

$$\dot{m}_{fuel,POR} = PR \cdot \dot{m}_{fuel,AB}. \tag{19}$$

The specific fuel consumption is calculated through

$$sfc = \frac{\dot{m}_{fuel,BC} + \dot{m}_{fuel,AB} + \dot{m}_{fuel,POR}}{F}, \tag{20}$$

where the delivered thrust is defined as

$$\begin{aligned} F = \dot{m}_9 \cdot c_9 + A_9 \cdot (p_9 - p_0) + \dot{m}_{19} \cdot c_{19} \\ + A_{19} \cdot (p_{19} - p_0) - \dot{m}_0 \cdot c_0. \end{aligned} \tag{21}$$

The main design parameters are listed in Table 1.

Turbofan Input Parameters		
Flight Mach number	M_0	0.80
Altitude	H_0	11 000 m
Fan pressure ratio	FPR	3.50
Fan isentropic efficiency	$\eta_{Fan,s}$	0.85
Overall pressure ratio	OPR	26
Compressor isentropic efficiency	$\eta_{C,s}$	0.85
Bypass ratio	BPR	0.40
Combustor isentropic efficiency	η_{BC}	1
Combustor total pressure recovery	π_{BC}	1
Turbine inlet temperature	$T_{t,4}$	1 800 K
Turbine isentropic efficiency	$\eta_{T,s}$	0.92
Augmentor exit temperature	$T_{t,7}$	2 000 K
Augmentor combustion efficiency	η_{AB}	0.90
Bypass nozzle total pressure recovery	$\pi_{By,N}$	1
Core nozzle total pressure recovery	π_N	1
Total Core Flow Equivalence Ratio	Φ_{Total}	0.90
Flameholderless System Input Parameters		
Power ratio	PR	0.14
Mass based equivalence ratio	Φ_{POR}	1.90
POR degree of fuel conversion	η_{POR}	1
POR total pressure recovery	π_N	1

TABLE 1: LIST OF MAIN DESIGN PARAMETERS.

System Weight Approximation

The weight approximation of the flame holder system is based on assumptions for both weight and total pressure loss of existing systems. It has been assumed that a typical flame holder system for an engine with an air mass flow rate of 80 kg/s introduces a total pressure loss of 5 % and weighs 50 kg. Concerning the variation of the total pressure loss, the system weight scales under the assumption that the total pressure loss is proportional to the area blockage A. Hence, the total pressure loss π varies with a reference length scale l squared. Volume, and therefore weight, is proportional to the reference length scale to the third power. As a result, the system weight scales with the total pressure loss to the power of three halves.

$$\pi \propto A \propto l^2, \quad m \propto l^3 \quad \Rightarrow \quad m \propto \pi^{3/2}$$

Since the air mass flow rate $\dot{m}$ also scales with the area A, the same consideration of the flame holder system weight applies for the scaling with the air mass flow rate, $m \propto \dot{m}^{3/2}$.

The weight approximation of the piping of the flame-holder-less design is carried out in accordance with the ASME code for pressure piping, ASME B31.3-2002, [7]. For good thermal resistance properties and to provide a conservative weight approximation, a dense nickel alloy has been chosen as pipe material, eg. INCOLOY 800H UNS N08810. The density of such a nickel alloy is on the order of $\rho = 8\,200\ kg/m^3$, [8], which again is a conservative estimate. The wall thickness t of a pipe section is calculated by equation (3a) of the ASME code and reads

$$t = \frac{p \cdot D_o}{2 \cdot (\underbrace{S \cdot E + p \cdot Y}_{modified\ strength})}, \tag{22}$$

where D_o, E, p, S, and Y represent respectively an outer pipe diameter, a material quality factor, an inner pressure, a material stress value and a material coefficient which accounts for the composition and the crystal structure of the material. The values used for E, S and Y are listed in Table 2.

Property	Upstream POR	Downstream POR
E	0.8	0.8
S	$f(T_{23})$ Table A-1, see [7].	6 894 757 Pa ($\widehat{=}$ 1 ksi)
Y	0.4	0.7

TABLE 2: PIPE MATERIAL N08810 PROPERTIES AT 1 172 K ACCORDING TO ASME B31.3, [7].

While the parameter values for the pipe upstream the POR are extractable from ASME B31.3, the expected temperature of the POx flowing through the piping downstream the POR exceeds the temperature range for which material properties are available. Although, this temperature could be above the melting temperature of the pipe material, it is assumed that the material is operated within the limits of its specifications, [7]. Without considering protection measures, a conservative estimate can be carried out by using the material properties for the highest listed temperature of 1 172 K. This is the condition where the material is weakest. The impact of the temperature becomes apparent through the modified strength, see Eq. 22, which at this temperature is only 6.2 % of its value at room temperature.

Furthermore, it should be noted that, according to ASME B31.3, special consideration of factors such as theory of failure, effects of fatigue, and thermal stress is required if the inner pipe pressure exceeds 2.12 MPa for a temperature of 1 172 K. As mentioned earlier, the pipe material is assumed to be operated within its specified limits; therefore, these factors have been neglected. Nevertheless, to provide a conservative weight approximation the wall thickness has been multiplied with a safety factor of 1.5, independent of the operating pressure.

As shown in Fig. 4, the pipe diameter between the POR and the augmentor injection points is smaller compared to that of the piping upstream the POR. This is due to 1) the reduced pressure from the acceleration of the flow after combustion, and 2) the division of the flow through what was assumed to be 6 augmentor injection locations. The dimensions of the piping necessary to calculate the overall weight of the piping can be found in Fig. 4. The weight of the POR is assumed to be equivalent to 50 % of the total weight of the piping necessary for the flame-holder-less system. The weight of the whole system scales with the bleed air mass flow rate, $m \propto \dot{m}^{3/2}$.

IV RESULTS AND DISCUSSION

This section presents the results of the comparison study between two augmentor flame stabilization concepts: the classic flame holder design versus the proposed flame-holder-less design presented above. The evaluation was carried out by comparing each design's influence on the engine's overall thermal efficiency, thrust and weight. Each performance parameter has been related to the total pressure loss due to the flame holders (flame holder design) and to the relative mass of air pulled from the compressor for the creation of POx mixtures (flame-holder-less design).

The thermal efficiency of an aero-engine is a quality measure of the conversion of energy stored in the fuel to energy available for the generation of thrust. Therefore, it is a good basis for the comparison between the common flame holder design and the new flame-holder-less concept. The thermal efficiency is defined as:

$$\eta_{th} = \frac{\frac{1}{2}\dot{m}_9 \cdot c_{9,id}^2 + \frac{1}{2}\dot{m}_{19} \cdot c_{19,id}^2 - \frac{1}{2}\dot{m}_0 \cdot c_0^2}{(\eta_{BC} \cdot \dot{m}_{fuel,BC} + \eta_{POR} \cdot \dot{m}_{fuel,POR} + \eta_{AB} \cdot \dot{m}_{fuel,AB}) \cdot LHV} \tag{23}$$

The velocity c_{id} describes the theoretical flow velocity when it is ideally expanded to atmospheric pressure.

Wet Operation

Figure 6 describes the effect that each design has on the thermal efficiency of the engine during wet operation. Figure 6a depicts the drop in η_{th} due to the total pressure loss caused by the flame holders. Figure 6b depicts the same effect caused by the extraction of air from the engine's core flow.

For the flame holder design, the total pressure loss decreases the velocity $c_{9,id}$. Therefore, according to Eq. 23, the drop in thermal efficiency becomes more pronounced with an increasing total pressure loss.

For the flame-holder-less concept, the bleed air mass flow rate has been altered by varying the POR-to-augmentor power ratio (PR) from 0 to 0.9 and adjusting the bleed air mass flow rate to maintain a constant Φ_{POR}. The peak PR value of 0.9 corresponds to 45 % of the total augmentor fuel being processed in the POR before injection into the augmentor. The peak PR value also corresponds to a required bleed air mass flow rate of 8 %. The solid line in Fig. 6b, or $0.3 \cdot OPR$, represents bleed air taken from the compressor at 30 % of the compressor's OPR. The dashed line in the same graph represents bleed air taken at the compressor exit, or 100 % of the OPR. It is noted that the η_{th} for the flame-holder-less design is not a strong function of the pressure at the point of air extraction, but rather on the bleed air mass flow rate. This is due to the fact that, between the two cases, the difference in momentum of the POx as it is injected in co-flow in the augmentor is negligible, and therefore the mass of air that is extracted has the dominant influence on thermal efficiency.

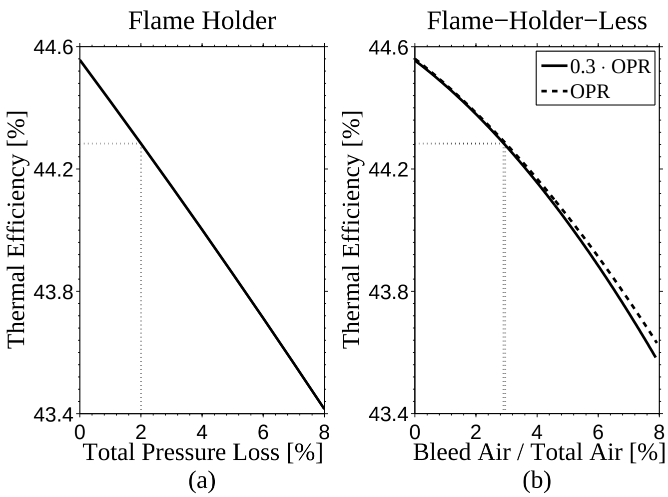

FIGURE 6: WET OPERATION THERMAL EFFICIENCY COMPARISON FOR a) COMMON FLAMEHOLDER DESIGN, AND b) FLAMEHOLDER-LESS DESIGN.

In comparing Fig. 6a and 6b, it is observed that even when the bleed air approaches 8 %, the reduction in thermal efficiency is comparable to a flame holder design with a total pressure loss of $4-6$ %. Thus, if flame stabilization can be achieved with the flame-holder-less system at a power ratio well below 0.9, the proposed concept should demonstrate higher thermal efficiency during wet operation. Such a comparison is highlighted in Fig. 6 using the dotted lines corresponding to $\eta_{th} = 44.3$ %. This η_{th} value corresponds to 2 % total pressure loss for the flame holder design. The corresponding efficiency is highlighted in Fig. 6b and is achieved with a bleed air percentage of approximately 3 %, corresponding to a power ratio of 0.24. As stated in Section I, the 2 % total pressure loss is estimated to be the minimum pressure loss of a flame holder system that will successfully stabilize augmentor combustion in the type of engine modeled here. Therefore, if the proposed concept can stabilize augmentor combustion under such conditions with a power ratio of 0.24 or less, it would enable the engine to achieve higher thermal efficiency during wet operation than the conventional flame holder design.

A similar analysis can be performed for any point along these curves. Along a different set of dotted lines, the bleed air mass flow rate can be found which could be extracted for the flame-holder-less system while maintaining the thermal efficiency of a given total pressure loss of the flame holder system. A relation between the flame holder total pressure loss and the bleed air mass flow rate as percentage of the total air mass flow rate can be found for both modeled cases of bleed air extraction locations:

$$\frac{\dot{m}_{23}}{\dot{m}_0} = 102.0408 \cdot \sqrt{0.0026 \cdot \pi_{FH} + 0.0063} - 8.0204 \qquad (24)$$

With this relation, the bleed air mass flow corresponding with 2 %, 4 % and 6 % total pressure loss is marked in Fig. 7 through Fig. 9 for both bleed air locations with X (2%), O (4%) and $+$ (6%) marks. The markers enable a system weight, thrust-to-weight and flight range comparison for a given thermal efficiency. It will be shown later that the proposed concept still provides improved performance even when designed to be operated at a point where the thermal efficiencies coincide. This improvement is due to the fact that the proposed concept, except for increasing engine weight, does not affect the engine during dry operation.

Dry Operation

For the flame holder design, the shape of the thermal efficiency curve during dry operation is similar to the one at wet operation, namely Fig. 6a. The total pressure loss inherent to the flame holders affects the flow in the same way during both operating schemes. However, the total thermal efficiency of the engine is shifted considerably higher during dry operation because all of the fuel is burned at near-peak pressure instead of a portion of total fuel being burned in the lower-pressure augmentor.

The flame-holder-less concept is assumed to introduce no total pressure loss at dry operation, so that its thermal efficiency corresponds to the thermal efficiency of the flame holder design at zero percent total pressure loss. Here, the main advantage of the new concept becomes apparent. During the long periods of dry operation, an engine equipped with the flame-holder-less augmentor operates at a higher efficiency, compared to one that operates using flame holders.

System Weight

As described by the model for the weight of the flame holder system in Section III, the system weight increases with the total pressure loss to the power of three halves. This is depicted in Fig. 7a.

The system weight of the flame-holder-less design depends on the mass flow rate, and the pressure and temperature of the bleed air. For a given mass flow rate the weight increases with pressure and temperature. This is due to the required larger wall thickness. The system is heaviest when the bleed air is taken from the exit of the compressor as shown by the dashed line in Fig. 7b. In contrast, the weight is lowest when the air is taken from the lower pressure bleed locations.

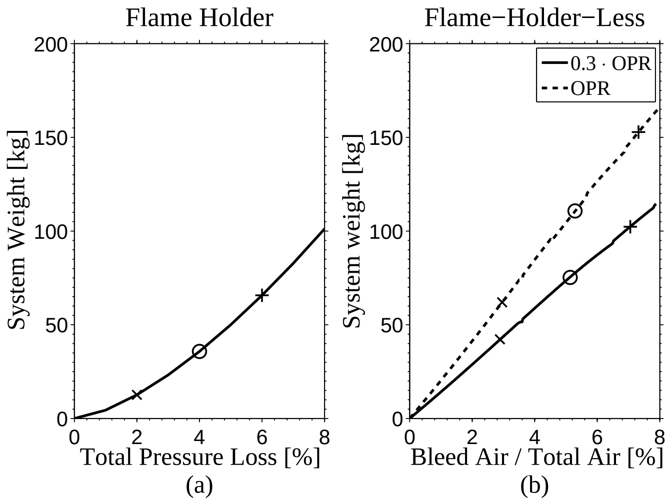

FIGURE 7: SYSTEM WEIGHT COMPARISON. a) COMMON FLAME-HOLDER DESIGN, AND b) FLAMEHOLDERLESS CONCEPT. X, O AND $+$ REPRESENT THE VALUES OF 2 %, 4 % AND 6 % TOTAL PRESSURE LOSS, OR, CONNECTED THROUGH A GIVEN THERMAL EFFICIENCY, THE VALUES OF THE CORRESPONDING BLEED AIR.

It should be mentioned that the impact of a variation of the piping length upstream the POR, as depicted in Fig. 4, is very small. This is due to the fact that the system weight is mainly determined by the heavy piping downstream the POR which requires a relatively large wall thickness due to thermal stress. For example, a decrease in pipe length of the low pressure case (30 % OPR) by 50 % reduces the system weight only by 2 %.

In comparison, the system weight of the flame-holder-less system may be less than the conventional flame holder design. The difference in their weights becomes more and more pronounced as the total pressure loss increases when compared to a flame-holder-less system designed for a bleed air mass flow rate of corresponding thermal efficiency. This result follows from the $3/2$ power relationship between flame holder weight and air mass flow in the engine. Even in its heaviest configuration, when the bleed air is taken from the end of the compressor, the system weight is less than that of the flame holder system. Note that a safety factor of 1.5 has been applied to the wall thickness of the piping for the flame-holder-less design for a more conservative weight estimate. As a final note, the length of the augmentor duct plays a significant role in determining engine weight. The criteria for determining at what bleed-air percentage the combustion process is stabilized should include stabilization occurring within the same duct length as the corresponding flame holder pressure drop. This criterion allows for the exclusion of duct length as a factor in system weight.

Thrust-to-Weight Ratio

The thrust-to-weight ratio combines the effects of the two designs on thrust delivered and on the overall engine weight. The design parameters of the engine such as total air mass flow rate of $\dot{m}_0 = 80\ kg/s$ are listed in Table 1 and an engine weight of $1\,000\ kg$ without flame-stabilization system has been assumed.

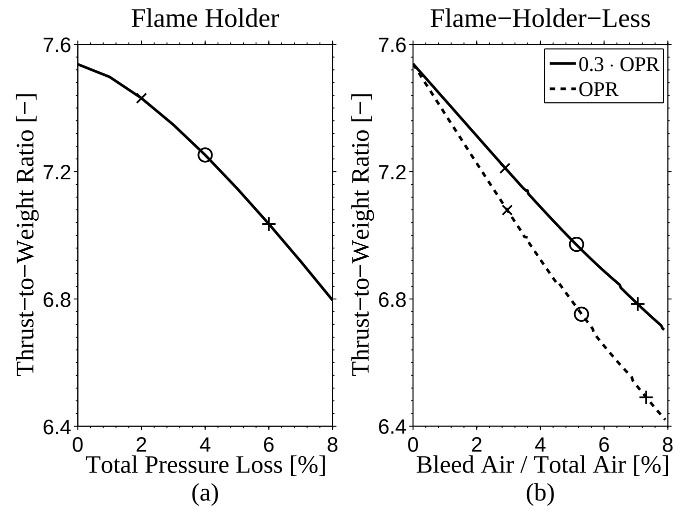

FIGURE 8: THRUST-TO-WEIGHT RATIO COMPARISON DURING WET OPERATION FOR a) COMMON FLAME HOLDER DESIGN, AND b) FLAME-HOLDER-LESS DESIGN. X, O AND $+$ REPRESENT THE VALUES OF 2 %, 4 % AND 6 % TOTAL PRESSURE LOSS, OR, CONNECTED THROUGH A GIVEN THERMAL EFFICIENCY, THE VALUES OF THE CORRESPONDING BLEED AIR.

Figure 8a and 8b show the thrust-to-weight ratio curves for the flame holder and flame-holder-less systems, respectively, during wet operation. For the flame holder system, an increasing total pressure loss reduces the specific thrust to drop as well as increases the system weight due to the increased size of the flame holders. These effects combine to create a relatively steep drop in the thrust-to-weight ratio curve versus the total pressure loss.

As can be seen from Fig. 8b, the drop in thrust-to-weight ratio has a smaller slope than that of the flame holder system. The marks of corresponding thermal efficiency (X, O and $+$) between the two graphs show that, despite having the same thermal efficiency, the flame-holder-less system performs at a higher thrust-to-weight ratio. This is due mostly to the fact that was demonstrated in Fig. 7, that the flame-holder-less system is lighter than the flame holder system with a corresponding thermal efficiency.

Flight Range

To investigate how the flame stabilization system of an aero-engine affects the performance of an aircraft, the dry operation flight range of a typical fighter aircraft has been determined. Although the flight range reflects many properties of an aircraft-engine configuration, the main parameters from an engine perspective are the specific fuel consumption and the total engine weight. The calculation of the flight range FR has been carried out according to Voit-Nitschmann, [9, p. 69].

$$FR = \frac{c_0 \cdot 1s}{sfc \cdot g \cdot 3600} \cdot \frac{c_L}{c_D} \cdot \ln \frac{m_{aircraft}}{m_{aircraft} - m_{fuel,burned}} \tag{25}$$

with

$$m_{aircraft} = m_{aircraft,empty} + m_{fuel,burned} + m_{engine} \tag{26}$$

For the derivation of this equation, steady flight has been assumed. Due to the decreasing fuel mass over time, the aircraft gains altitude starting from 11 000 m. c_0, sfc, $\frac{c_L}{c_D}$, $m_{aircraft}$, $m_{aircraft,empty}$, m_{engine} and $m_{fuel,burned}$ represent a flight velocity, a specific fuel consumption, a lift-to-drag ratio, a total aircraft mass, an aircraft mass without fuel and engines, an engine mass and a mass of consumed fuel, respectively. With $c_0 = 236\ m/s \mathrel{\widehat{=}} M = 0.8$, $\frac{c_L}{c_D} = 10$, $m_{aircraft,empty} = 9\,000\ kg$ and $m_{fuel,burned} = 5\,000\ kg$, reasonable values for a modern fighter aircraft have been chosen.

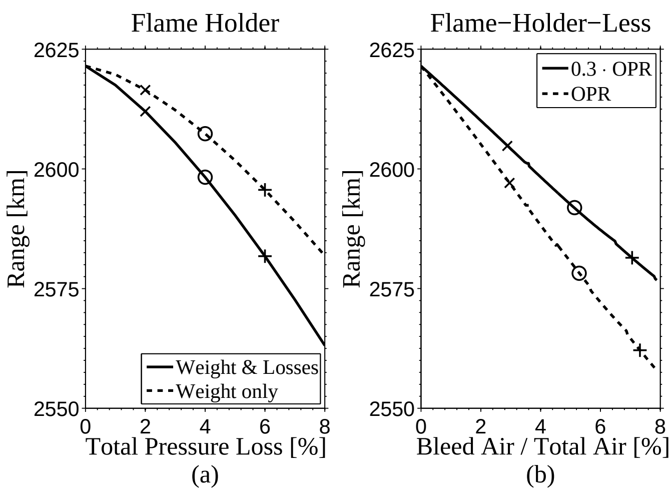

FIGURE 9: FLIGHT RANGE COMPARISON FOR a) COMMON FLAMEHOLDER DESIGN, AND b) FLAMEHOLDERLESS DESIGN. X, O AND $+$ REPRESENT THE VALUES OF 2 %, 4 % AND 6 % TOTAL PRESSURE LOSS, OR, CONNECTED THROUGH A GIVEN THERMAL EFFICIENCY, THE VALUES OF THE CORRESPONDING BLEED AIR.

As Fig. 9a shows, the range of the aircraft equipped with the flame holder design is reduced slightly with increasing total pressure loss. Utilizing the flame holder concept, the aircraft has to carry the extra weight and suffers under increased specific fuel consumption due to the obstacles partially blocking the flow path in the afterburner cavity. The solid line in Fig. 9a represents the total effect of the flame holders on flight range, including the reduced thrust due to pressure losses and the increased aircraft weight. The dashed line in Fig. 9a represents the effect of the flame holders on aircraft weight only, neglecting effects due to total pressure loss. It should be noted that the dashed line in Fig. 9a indicates a lower flight range than its corresponding points for the flame-holder-less system in Fig. 9b. This performance improvement from the flame-holder-less design is expected since system weight is reduced and, during dry operation, the system does not affect the engine's thrust or thermal efficiency.

V CONCLUDING REMARKS

A thermal analysis has shown the potential merits of the proposed flame-holder-less flame stabilization concept. The proposed concept achieves comparable performance during wet operation, while providing superior performance during dry operation. This dry-operation performance enhancement is achieved through the reduction of total pressure losses which reduce the engine's thermal efficiency and thrust. As expected from these results, thrust and specific fuel consumption between the two concepts are well-matched during wet operation and improved during dry operation. The results suggest that the flame-holder-less concept may be able to deliver equal or superior performance to the flame holder concept.

It should be noted that, as was explained in Section I, eliminating the presence of bluff body flame holders carries other advantages. Removing the flame holders from the engine while maintaining a stable thrust augmentor may also reduce the potential for acoustic instabilities such as "screech" and reduce the overall visibility of the aircraft to detection devices. If the concept suggested here can be proven to be an effective method of flame stabilization, these advantages may suggest that the flame-holder-less concept would be more desirable than using flame holders.

Further work needs to continue to determine certain requirements for the concept to successfully maintain the augmentor combustion process, such as what minimum POR-to-augmentor power ratio and what POx and augmentor fuel injection schemes achieve the best stabilization. Other design considerations include, for example, necessary thermal management and system cooling, heat transfer from the POR to other engine components, and operability effects of drawing air from the compressor. However, the results presented in this study suggest that such a system may be an effective alternative to the conventional flame holder concept.

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