



## Evangelia Pontika<sup>1</sup>

Centre for Propulsion and Thermal Power  
Engineering,  
Faculty of Engineering and Applied Sciences,  
Cranfield University,  
Cranfield, Bedford MK430AL, UK  
e-mail: [evangelia.pontika@cranfield.ac.uk](mailto:evangelia.pontika@cranfield.ac.uk)

## Panos Laskaridis

Centre for Propulsion and Thermal Power  
Engineering,  
Faculty of Engineering and Applied Sciences,  
Cranfield University,  
Cranfield, Bedford MK430AL, UK  
e-mail: [p.laskaridis@cranfield.ac.uk](mailto:p.laskaridis@cranfield.ac.uk)

## Daniel Ewald

Institute for Applied Materials – Electrochemical  
Technologies (IAM-ET),  
Karlsruhe Institute of Technology (KIT),  
Karlsruhe 76131, Germany  
e-mail: [daniel.ewald@kit.edu](mailto:daniel.ewald@kit.edu)

## André Weber

Institute for Applied Materials – Electrochemical  
Technologies (IAM-ET),  
Karlsruhe Institute of Technology (KIT),  
Karlsruhe 76131, Germany  
e-mail: [andre.weber@kit.edu](mailto:andre.weber@kit.edu)

# Cycle Potential and Aerothermal Impact of SOFC Integration Into the Gas Turbine Cycle

*Hydrogen gas turbine (GT)-solid oxide fuel cell (SOFC) cycles for aviation can offer a step change in efficiency and  $\text{NO}_x$ . The SOFC contributes electrical power but is also integrated within the GT cycle to receive air and return hot gases. This paper introduces normalized coupling parameters and integration constraints to perform a systematic thermodynamic exploration of GT-SOFC cycles with three main outcomes: (a) impacts of SOFC integration on the GT cycle gas composition and thermodynamics, (b) the potential of different cycles from SOFC integration, and (c) universal coupling maps for the cycle potential. The overall efficiency change was found to be a nearly unique function of the electrical degree of hybridization ( $\text{DoH}_{el}$ ) due to the high electrothermal potential of the SOFC, while the main enabler of high  $\text{DoH}_{el}$ —high efficiency is combining low GT specific power with high SOFC specific power. Within the investigated range of air utilization (AU) and cell voltage, low specific power GT-SOFC cycles can have specific fuel consumption (SFC) reduction up to  $-55.7\%$  with optimized overall pressure ratio (OPR) and up to  $-53\%$  with constrained OPR = 10 due to SOFC technology. High specific power GT-SOFC cycles can have SFC reduction up to  $-30\%$  with optimized OPR and up to  $-14.1\%$  with constrained OPR, when comparing against  $\text{H}_2\text{GT}$  cycles with the same turbine entry temperature (TET) and optimized OPR. With constrained OPR, low specific power GT-SOFC cycles reach higher efficiencies than high specific power GT-SOFC cycles. When  $\text{AU} = 0.1$ , OPR-constrained high specific power GT-SOFC cycles perform worse than the  $\text{H}_2\text{GT}$  cycle with optimized OPR, and increasing AU should be a priority. [DOI: 10.1115/1.4072417]*

**Keywords:** hydrogen, steam injection, hybrid electric, aerospace, aviation

## 1 Introduction

**1.1 Motivation.** In recent years, fuel cell (FC) systems and electrification for aviation have attracted increasing interest. Electric powertrains (i.e., motor–inverter–converter) have high combined efficiencies ( $\sim 85$  to  $95\%$ ), while hydrogen-powered fuel cells combine good efficiency ( $\sim 40$  to  $60\%$ ), often better than small aero gas turbine (GT) engines ( $\sim 25$  to  $30\%$ ) and have zero in-flight carbon and  $\text{NO}_x$  emissions. Solid oxide fuel cells (SOFCs) operate at higher operating temperatures than other types of fuel cells, and their by-products are high-temperature steam, oxygen-depleted air, and unutilized hydrogen. Apart from the electrification of power, there is an opportunity to recover the high-quality SOFC heat waste in the GT cycle, use warmer hydrogen for GT combustion, and inject the steam to reduce the GT  $\text{NO}_x$  emissions and improve the gas turbine and overall system efficiency.

**1.2 Gas Turbine-Solid Oxide Fuel Cell Cycle Performance Explorations.** There are studies in GT-SOFC cycles for stationary power generation applications [1], and recently GT-SOFC cycles have attracted interest in aerospace. A fraction of the studies propose GT-SOFC for replacing the auxiliary power unit [2], and more

recent literature also proposed the GT-SOFC as the main propulsion system.

Mashamba et al. [3] analyzed the off-design performance sensitivity of a GT-SOFC turbofan performance to the variation of certain SOFC operating parameters: oxygen-to-carbon ratio of the reformer, the fuel utilization factor of the SOFC, and the high-pressure compressor (HPC) outlet air split ratio to the reformer, SOFC cathode, and GT combustor. The design of the GT and SOFC was fixed with a GT design point combustor outlet temperature at 1758 K, and the sensitivity analysis was at off-design. Benchmarking against the baseline was not included, and the comparison was focused on the aforementioned SOFC operating parameter variations.

Collins et al. [4] proposed an SOFC-GT-battery system to replace the engine of three different aircraft: B787-8, A380, A300, and F70. A tradeoff analysis between current density (associated with the FC area and efficiency), compressor pressure ratio ( $\text{PR} = 15$ – $40$ ), and payload capacity (associated with the system weight) was produced. The compressor and turbine masses were estimated using a NASA correlation for microgas turbines with PR up to 6 and lower mass flow up to 1.5 kg/s. The payload was maximized for an intermediate current density of around 1 A/cm<sup>2</sup>. The outcomes of the study were the system efficiency, payload capacity change for each aircraft platform, the mass of the system, and the component mass breakdown.

Kohler et al. [5] assessed GT-SOFC layouts for 4 MW turboprops with regard to the heat source position of the heat exchanger (HEX) that preheats the incoming SOFC air and hydrogen at cruise design point under constant SOFC conditions. The overall pressure ratio

<sup>1</sup>Corresponding author.

Manuscript received April 21, 2026; final manuscript received July 13, 2026; published online August 28, 2026. Assoc. Editor: Brian Bohan.

(OPR) was fixed so that the required SOFC inlet pressure at cruise can be met (1 atm). The turbine entry temperature (TET) and the power share between the GT and SOFC were changed simultaneously for each HEX layout so that all the air of the GT is used at the SOFC. The outcome of the study was a comparison of the overall efficiency and thermal conductance (UA) of the HEX for the different HEX heat source positions. The overall efficiency was estimated at 51.5% when using SOFC off-gas at the HEX hot side combined with a TET of 700 K, and at 68.3% when using the low-pressure turbine (LPT) exhaust combined with a TET of 1250 K, but without a baseline gas turbine as a benchmark. Using the gas downstream of the turbines resulted in higher overall efficiency than using SOFC or combustor outlet gases; however, due to the lower temperature at the turbine section outlet, the hydrogen may not be preheated sufficiently, and the required UA was increased. Therefore, the contribution of this study [5] was an architectural exploration with variable HEX positions rather than a systematic coupled thermodynamic exploration with different combinations of GT and SOFC design parameters.

Kohler et al. [6] investigated the coupling of GT and SOFC systems, and the resulting system efficiencies for a regional application with 1.2 MW at cruise. A specific SOFC cell was used with a fixed temperature constraint, and the TET was used as a handle to match the required airflow of the SOFC with an upper limit of  $TET = 1400\text{ K}$ . When the temperature limit of the GT was reached while decreasing the electrical power share, then the option of a bypass was introduced. A new metric was also introduced: the corrected electrical power share, which combines the required SOFC power and number of cells, serving as an indicator of the mass increase when the electrical power share changes or the design point cell voltage changes. The coupling was done within a narrow design envelope, and detailed aerothermal effects in the gas turbine were not the focus of the study. Overall, their approach was to handle the GT TET to match the air supply requirements of a given cell design and technology, while the present paper will examine the coupling problem from a GT cycle perspective using a nondimensional coupling methodology to answer: what are the fundamental thermodynamic impacts on the GT cycle, how different SOFC and GT design regions can be combined, and how the SOFC should evolve to achieve different levels of improvement for different cycles. Kohler et al. [6] report a maximum system efficiency of 67%, but without a GT-only benchmark.

Geuther et al. [7] assessed the feasibility of a hybrid GT-SOFC-battery system. There were two architecture variations: one with hot recycle blowers and one with a series of HEX and low-temperature blowers, with the former option resulting in higher overall efficiency. The variables were the operating current density, voltage (number of cells), altitude, and battery energy density. For the hot recycle blower architecture, the maximum overall efficiency was found at low to intermediate current densities ( $0.3\text{ A/cm}^2$ ), and the optimum current density was increasing with reducing altitude. Other outcomes of the study were the overall system efficiency sensitivity to the altitude variation, and the calculation of the system mass, gross mass, and fuel mass at varying hybridization percentage and range, including a comparison to a conventional regional aircraft.

Waters et al. [8] performed a sensitivity analysis of the fuel flowrate of a GT-SOFC turbofan engine for high-altitude, long-endurance (HALE) unmanned air vehicle (UAV) and regional aircraft to various GT and SOFC input parameters, within set GT and SOFC designs. They also considered two levels of SOFC power combined with two SOFC positioning options: one that grew the engine radially and one axially. The resulting drag was compared between the SOFC positioning options, and the fuel flowrate of the GT-SOFC turbofan was compared against the fuel flowrate of mechanically driven generators.

Freeh et al. [9] analyzed the performance of a GT-SOFC replacement of a 300-pax aircraft auxiliary power unit (APU). The analysis included operating points with 440 kW electrical power at takeoff, 440 kW at cruise, and 250 kW at cruise within set component design. They demonstrated the change of the

component-level operating points due to the different electric power levels and stressed the need for multipoint assessments to select the appropriate design point, which in their case was takeoff. Benchmarking against a baseline was not included.

Wagner et al. [10] analyzed the performance of a GT-SOFC turbofan, based on JT9D, running on ammonia/hydrogen fuel blends, including a reactor to decompose ammonia to hydrogen and preheating the SOFC gases using the low-pressure turbine heat. The sensitivity analyses focused on the variation of the combustion equivalence ratio, the ammonia/hydrogen ratio, the altitude, the Mach number, the fan pressure ratio, and the bypass ratio. However, the TET of the GT and the number of stacks/cells were kept constant, leading to variable net thrust and power for each case. The jet A conventional turbofan was reported with an overall efficiency of 31.1%, and the best overall efficiency was calculated for the pure hydrogen GT-SOFC at 35.4%.

Mantelli et al. [11] compared GT-SOFC layouts for commuter application with design point analysis at takeoff and cruise and an investigated degree of hybridization (DoH) between 5 and 20%, while the GT design parameters were constant. The two layouts focused on pressure management solutions (expansion valve versus mild pressurization) of the bleed flow that feeds the SOFC. The efficiency for both layouts and both design points at  $DoH = 20\%$  was in the range of 4–5% improvement from the baseline, which was a turboprop with  $\sim 28\%$  efficiency at takeoff design point and  $\sim 30\%$  at cruise design point.

**1.3 Contribution.** The existing literature on GT-SOFC cycles focuses mostly on overall assessment metrics for specific applications, while the GT-SOFC coupling seems mostly intuitive, using set GT and/or SOFC designs with sensitivity analysis to selected parameter variations but without a systematic exploration of the thermodynamic design space using normalized coupling parameters. Some studies have focused on sensitivity analysis and operational parameter optimization within a set design without benchmarking against a baseline without the SOFC. The aerothermal effect of the SOFC integration into the GT cycles has not been fully explored, as well as the full envelope potential for a range of cycles that could represent different aircraft applications.

This paper proposes a novel coupled thermodynamic exploration and cycle potential analysis of hydrogen GT-SOFC covering the full design envelope for turboshaft aero-engine applications and comparison against GT-only solutions to justify the case for SOFCs. The degrees-of-freedom being exploited in this analysis include the OPR and TET of the GT, the ratio of the GT air being diverted to the SOFC (BleedRatio), the ratio of unutilized SOFC  $H_2$  to the total  $H_2$  needed in the GT combustor ( $H_2$ ratio), the thermodynamic degree of hybridization ( $DoH_{thermo}$ ) and the electrical degree of hybridization ( $DoH_{el}$ ). It becomes particularly interesting how different combinations of ( $DoH_{thermo}$ ) and ( $DoH_{el}$ ) can be achieved by manipulating design variables and technology variables of the subsystems, opening up a new design space. The main contributions of the paper can be summarized as:

- The aerothermal impact of the SOFC integration on the GT gas path properties, GT cycle performance, and overall performance is quantified and analyzed over a wide range of design parameters for the GT and the SOFC.
- A unique relationship of the efficiency change with the  $DoH_{el}$  is identified irrespective of the SOFC and GT parameters, which is explained by formulating and analyzing the electrothermal power potential of the SOFC.
- Universal coupling maps are produced to rapidly identify the maximum potential benefit in terms of absolute efficiency change as a function of GT and SOFC design parameters covering a wide design and technology envelope to inform the direction of future design and technology development.
- The GT design regions and maximum coupling potential are related to four aero-engine turboshaft applications, and technology and sizing considerations are introduced.



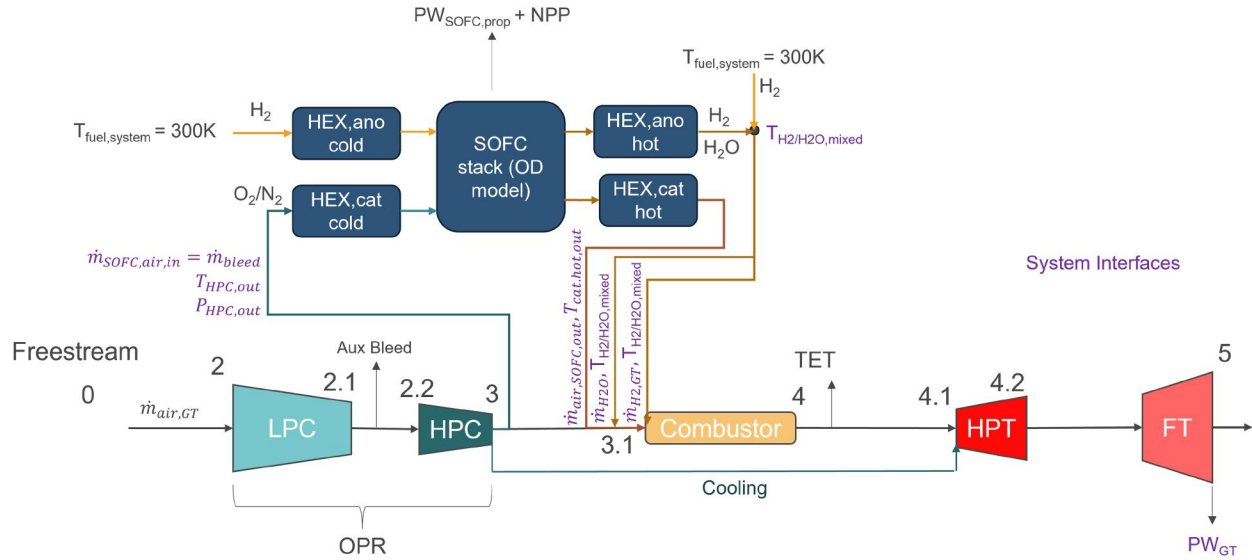


Fig. 2 Hybrid GT performance model with system interfaces

$$PW_{IPPS} = PW_{GT} + PW_{el,prop} \quad (1)$$

where the power split is defined with an electrical degree of hybridization of the total propeller power (Eq. (2))

$$DoH_{el} = \frac{PW_{el,prop}}{PW_{IPPS}} = \frac{PW_{el,prop}}{PW_{GT} + PW_{el,prop}} \quad (2)$$

Also, to improve the efficiency of the GT any power-off takes for (auxiliary) nonpropulsive power (NPP) is removed from the GT, and this is provided by the SOFC (Eq. (3)).

$$PW_{SOFC} = PW_{SOFC,el,prop} + PW_{aux} \quad (3)$$

where the SOFC power contributing to propulsion needs to account for the losses at the electric powertrain (Eq. (4))

$$PW_{SOFC,el,prop} = \frac{PW_{el,prop}}{\eta_{el,powertrain}} \quad (4)$$

The auxiliary power to the aircraft, also known as NPP can be defined relative to the total propeller power ( $PW_{IPPS}$ ) in the following equation using a relative power penalty factor for nonpropulsive (auxiliary) power (RNPP):

$$PW_{aux} = PW_{IPPS} \cdot RNPP \quad (5)$$

**2.5 Thermodynamic Coupling Variables.** Normalized coupling parameters and metrics are preferred to decouple the methods from the size and power requirement iterations. The thermodynamic coupling can be described with a number of normalized parameters. First, the BleedRatio describes what fraction of the total HPC outlet flow is diverted through the SOFC (Eq. (6))

$$BleedRatio = \frac{\dot{m}_{air,SOFC,in}}{\dot{m}_{air,HPC,out}} \quad (6)$$

where  $\dot{m}_{HPC,out,air}$  is the available GT air flow to bleed to the SOFC after any auxiliary bleed and cooling bleed. The  $H_2Ratio$  describes what fraction of the hydrogen mass flow needed for the GT combustion is covered by the unutilized SOFC hydrogen (Eq. (7))

$$H_2Ratio = \frac{\dot{m}_{SOFC,H_2,unused}}{\dot{m}_{GT,H_2,total}} \quad (7)$$

where the total  $H_2$  required by the GT combustor to produce a target power is provided by the SOFC unutilized  $H_2$ , and the remaining is supplemented by the fuel system (Eq. (8))

$$\dot{m}_{GT,H_2,total} = \dot{m}_{SOFC,H_2,unused} + \dot{m}_{H_2,fuelsystem} \quad (8)$$

The contributed steam from the SOFC to the GT can be expressed relative to the GT air flow, which is broadly known as water/steam to air ratio (WAR) (Eq. (9))

$$WAR = \frac{\dot{m}_{H_2O,SOFC}}{\dot{m}_{air,GT}} \quad (9)$$

A thermodynamic degree of hybridization can be defined considering the enthalpy change per species in the SOFC to the total enthalpy rise required from the HPC outlet to the combustor outlet (Eq. (10)). This is a new metric that expresses the thermodynamic contribution of the SOFC to the GT cycle

$$DoH_{thermo} = \frac{\Delta \dot{H}_{SOFC}}{\Delta \dot{H}_{total}} = \frac{\sum \dot{H}_{i,SOFC,out} - \sum \dot{H}_{i,SOFC,in}}{\sum \dot{H}_{i,Comb,out} - \sum \dot{H}_{i,HPC,out}} \quad (10)$$

An alternative, reduced version can be deduced using the enthalpy contribution of the SOFC to the total enthalpy rise that would be required by the GT combustor (Eq. (11))

$$\begin{aligned} DoH_{thermo} &= \frac{\Delta \dot{H}_{SOFC}}{\Delta \dot{H}_{total}} \\ &= \frac{(\dot{m}_{SOFC,air} + \dot{m}_{SOFC,H_2}) \cdot Cp \cdot (T_{cat,HEX,out} - T_{HPC,out})}{\dot{m}_{total} \cdot Cp \cdot (TET - T_{HPC,out})} \end{aligned} \quad (11)$$

where  $\dot{m}_{total}$  is the total mass flow of the SOFC and GT.

The thermodynamic degree of hybridization expresses how much enthalpy contribution the SOFC is making to the GT cycle, and the electrical degree of hybridization expresses what percentage of the propeller input shaft power comes from the electric motor.

**2.6 Integration Constraints.** The  $DoH_{thermo}$  is constrained with Eq. 12. When  $DoH_{thermo} = 1$  the need for combustion is eliminated because the total enthalpy rise that was previously required by the GT combustor can be done by the SOFC. If  $DoH_{thermo} > 1$ , although the system can be physically realized, the

excess combustor outlet enthalpy will lead to excess free turbine power. This is an extreme case that would require a high SOFC system outlet temperature

$$0 \leq \text{DoH}_{\text{thermo}} \leq 1 \quad (12)$$

The maximum value for the BleedRatio is 1 (Eq. (13)), and it expresses a transition to an architecture where all the available GT air flow goes through the SOFC

$$0 \leq \text{BleedRatio} \leq 1 \quad (13)$$

The GT performance simulation resolves the combustor enthalpy balance to meet a target TET, and therefore, calculates the required fuel flow. In this hybrid system, this fuel flow required by the GT combustor is provided through the unutilized SOFC hydrogen and the remaining is provided by the fuel system. However, it should always be checked that the unutilized SOFC hydrogen is lower than the total required by the GT combustor enthalpy balance (Eq. (14)) otherwise, excess power will be produced by the GT.

An  $\text{H}_2\text{Ratio}$  over 1, which means the SOFC provides excess unutilized hydrogen, can be overcome by reducing the  $\text{DoH}_{\text{el}}$ , increasing the SOFC FU factor (up to a maximum value of 0.95), or by designing the GT with lower TET which will reduce its baseline thermal efficiency. The constraint of Eq. (14) applies only to architectures with  $\text{DoH}_{\text{thermo}} < 1$  where combustion takes place

$$0 \leq \text{H}_2\text{Ratio} \leq 1 \quad (14)$$

It should be noted that the  $\text{DoH}_{\text{thermo}}$  and  $\text{H}_2\text{Ratio}$  constraints are imposed constraints on the system so that the total power requirement is met with no waste of hydrogen energy, but they are not physical constraints of the system. The BleedRatio can be classified as a physical constraint of the system as it is tied to the airflow availability of the system.

Finally, it is also suspected that there is a limit in the combustion stability as a function of the steam WAR and oxygen consumption in the SOFC, but this cannot be quantified at this stage in the absence of relevant experimental data. This is included only for completeness, and future combustion experimental studies should investigate this limit.

$$\text{FAR}_{\text{limit}} = f(\text{WAR}, y_{\text{O}_2}) \quad (15)$$

## 2.7 Coupled Electrothermodynamic Analysis

**2.7.1 Derivation of Analytical Relationship Between  $\text{DoH}_{\text{el}}$  and BleedRatio.** An analytical relationship between the  $\text{DoH}_{\text{el}}$  and the BleedRatio can be formed as a function of the GT and SOFC design parameters. The derivation starts by equating the required SOFC air flow with the bleed extracted from the outlet of the HPC after the extraction of cooling and auxiliary bleeds (Eq. 16).

$$\dot{m}_{\text{GT,air,in}} \cdot (1 - m_{\text{c,rel}}) \cdot \text{BleedRatio} = \dot{m}_{\text{SOFC,air,in}} \quad (16)$$

The GT air flow can be replaced with a relationship between GT power and GT specific power ( $\text{SP}_{\text{GT}}$ ) in terms of power per air flow (Eq. (17)). The GT specific power is a function of the TET, OPR, and nozzle pressure ratio, with a stronger impact from the TET. After the GT cycle is thermodynamically integrated with the SOFC, if the same TET is maintained, the specific power is slightly improved due to the higher steam content

$$\dot{m}_{\text{GT,air,in}} = \frac{\text{PW}_{\text{GT}}}{\text{SP}_{\text{GT}}} \quad (17)$$

Using Faraday's law, a specific power in terms of power per air flow can be formulated for the SOFC as a function of its AU and Ucell (Eq. (18))

$$\text{SP}_{\text{SOFC}} = \frac{\text{PW}_{\text{SOFC}}}{\dot{m}_{\text{air,SOFC,in}}} = \frac{\text{Ucell} \cdot z_{\text{O}_2} \cdot F \cdot \text{AU} \cdot y_{\text{O}_2}}{\text{MW}_{\text{air}}} \quad (18)$$

Replacing Eqs. (17) and (18) in Eq. (16)

$$\frac{\text{PW}_{\text{GT}}}{\text{SP}_{\text{GT}}} \cdot (1 - m_{\text{c,rel}}) \cdot \text{BleedRatio} = \frac{\text{PW}_{\text{SOFC}}}{\text{SP}_{\text{SOFC}}} \quad (19)$$

The relationship between  $\text{PW}_{\text{GT}}$  and  $\text{PW}_{\text{SOFC}}$  can be normalized with reference to the  $\text{PW}_{\text{IPPS}}$  using the  $\text{DoH}_{\text{el}}$ , the electric powertrain efficiency and relative power penalty factor for the nonpropulsive (auxiliary) power ( $R_{\text{NPP}}$ ), therefore, Eq. (19) becomes

$$\begin{aligned} & \frac{\cancel{\text{PW}_{\text{IPPS}}} \cdot (1 - \text{DoH}_{\text{el}})}{\text{SP}_{\text{GT}}} \cdot (1 - m_{\text{c,rel}}) \cdot \text{BleedRatio} \\ &= \frac{\cancel{\text{PW}_{\text{IPPS}}} \cdot \left( \frac{\text{DoH}_{\text{el}}}{\eta_{\text{el,powertrain}}} + R_{\text{NPP}} \right)}{\text{SP}_{\text{SOFC}}} \end{aligned} \quad (20)$$

Equation (20) can be used to find the  $\text{DoH}_{\text{el}}$  for a given BleedRatio, where the maximum  $\text{DoH}_{\text{el}}$  can be achieved for BleedRatio = 1. Alternatively, specific power targets can be found to meet the desired  $\text{DoH}_{\text{el}}$ .

**2.7.2 Overall Efficiency Change Potential.** An analytical relationship between overall efficiency improvement and  $\text{DoH}_{\text{el}}$  can be derived by replacing the baseline GT combustion heat release with the heat release of the SOFC and the enthalpy contribution due to the additional mass of the SOFC products (Eq. (21)). The enthalpy contribution due to the additional product mass can be approximated with the term  $(\dot{H}_{\text{SOFC,H}_2\text{O}} - \dot{H}_{\text{SOFC,O}_2})$  at the temperature at which the reaction is assumed to occur (before the heat release). The enthalpy of the oxygen is removed as it was already present in the GT gas path. At the time of the exothermic reaction, there is a simultaneous conversion of  $\text{H}_2$  to  $\text{H}_2\text{O}$  and heat release, therefore, it is hard to determine a temperature at the time of the  $\text{H}_2\text{O}$  formation before any heat is absorbed. It is assumed that the  $\text{H}_2$  enters the system at 300 K (fuel system temperature) and the  $\text{O}_2$  temperature at the SOFC system inlet can be in the range of 400–900 K depending on the gas turbine OPR and ambient conditions, therefore, the enthalpy at 500 K will be assumed for the  $\text{H}_2\text{O}$  and  $\text{O}_2$  calculation in Eq. (21) to calculate a conservative, generic efficiency change potential irrespective of the cycle. However, when the user of the method already knows the type of cycle, they can use a more appropriate assumption for the  $\text{H}_2\text{O}$  phase and temperature for the following equation:

$$\begin{aligned} \Delta \dot{H}_{\text{GT,hybrid}} &= \Delta \dot{H}_{\text{GT,baseline}} - \Delta \dot{H}_{\text{SOFC,heat}} \\ &\quad - (\dot{H}_{\text{SOFC,H}_2\text{O}} - \dot{H}_{\text{SOFC,O}_2}) \end{aligned} \quad (21)$$

The  $\Delta \dot{H}_{\text{GT,baseline}}$ , which equals  $\dot{m}_{\text{f,baseline}}$  can be calculated using the baseline GT efficiency and the GT power output (Eq. (23))

$$\eta_{\text{baseline}} = \frac{\text{PW}_{\text{GT}}}{\Delta \dot{H}_{\text{GT,baseline}}} = \frac{\text{PW}_{\text{IPPS}} \cdot (1 - \text{DoH}_{\text{el}})}{\Delta \dot{H}_{\text{GT,baseline}}} \quad (22)$$

Equation (22) can be transformed to Eq. (23) when the GT power and the baseline efficiency are known

$$\Delta \dot{H}_{\text{GT,baseline}} = \frac{\text{PW}_{\text{GT}}}{\eta_{\text{baseline}}} = \frac{\text{PW}_{\text{IPPS}} \cdot (1 - \text{DoH}_{\text{el}})}{\eta_{\text{baseline}}} \quad (23)$$

The heat release of the SOFC  $\Delta \dot{H}_{\text{SOFC,heat}}$  is approximated using the electrical efficiency of the SOFC (Eq. (24))

$$\begin{aligned}\Delta\dot{H}_{\text{SOFC,heat}} &= \frac{PW_{\text{SOFC}}}{\eta_{\text{SOFC}}} \cdot (1 - \eta_{\text{SOFC}}) \\ &= PW_{\text{IPPS}} \cdot \left( \frac{\text{DoH}_{\text{el}}}{\eta_{\text{el,powertrain}}} + R_{\text{NPP}} \right) \cdot \frac{(1 - \eta_{\text{SOFC}})}{\eta_{\text{SOFC}}}\end{aligned}\quad (24)$$

where the electrical efficiency of the SOFC can be described as a function of the Ucell with Eq. (25) by transforming the equation for the consumed SOFC hydrogen (Eq. (A7) in Appendix)

$$\eta_{\text{SOFC}} = \frac{U_{\text{cell}} \cdot z_{\text{H}_2} \cdot F}{MW_{\text{H}_2} \cdot \text{LHV}_{\text{H}_2}} \quad (25)$$

After the  $\Delta\dot{H}_{\text{GT,hybrid}}$  is determined analytically with Eqs. (21)–(25), the overall efficiency change can be approximated with the following equation:

$$\Delta\eta_{\text{IPPS}} = \frac{PW_{\text{IPPS}}}{\Delta\dot{H}_{\text{GT,hybrid}} + \dot{m}_{\text{H}_2,\text{SOFC,consumed}} \cdot \text{LHV}} - \eta_{\text{GT,baseline}} \quad (26)$$

**2.7.3 Electrothermal Power Potential of the Solid Oxide Fuel Cell.** Within the investigated baseline cycle efficiencies, the overall efficiency change was found nearly independent of the GT cycle and SOFC parameters, but it is a strong function of the  $\text{DoH}_{\text{el}}$  as it will be demonstrated in Sec. 4.

To explain this trend, a new metric called electrothermal power potential of the SOFC was derived for different  $\text{DoH}_{\text{el}}$  and SOFC efficiencies (thus Ucell). This power potential expresses the potential of the SOFC to produce electric motor power for the propeller, nonpropulsive power for the aircraft, and GT shaft power through the enthalpy contribution to the GT (Eq. (27)). The electric

motor power considers the SOFC stack electrical efficiency and the electric powertrain efficiency, and the potential for shaft power generation at the GT considers the turbine efficiencies. Of course, there are some further, small losses, such as the heat waste at the nozzle, heat losses to the environment, and small offsets due to the Cp changes, but this power potential serves as a good approximation of the theoretical potential and explanation of the overall efficiency change being nearly independent of the Ucell and the cycle

$$PP_{\text{el+th}} = \frac{PW_{\text{el,prop}} + PW_{\text{aux}} + PW_{\text{thermal,shaft}}}{\dot{m}_{\text{H}_2,\text{SOFC,consumed}} \cdot \text{LHV}} \quad (27)$$

where the  $PW_{\text{el,prop}}$  and  $PW_{\text{aux}}$  terms can be written as function of the IPPS power, the electric powertrain efficiency and the non-propulsive power relative to the propulsive power ( $R_{\text{NPP}}$ ). The potential for shaft power generation at the GT can be written as the heat release of the SOFC multiplied by the turbine efficiencies (Eq. (28))

$$\begin{aligned}PP_{\text{el+th}} &= \frac{PW_{\text{IPPS}} \cdot \text{DoH}_{\text{el}} + PW_{\text{IPPS}} \cdot R_{\text{NPP}}}{\dot{m}_{\text{H}_2,\text{SOFC,consumed}} \cdot \text{LHV}} \\ &\quad + \frac{\Delta\dot{H}_{\text{SOFC,heat}} \cdot \eta_{\text{HPT}} \cdot \eta_{\text{FT}}}{\dot{m}_{\text{H}_2,\text{SOFC,consumed}} \cdot \text{LHV}}\end{aligned}\quad (28)$$

The  $R_{\text{NPP}}$  represents the fraction of the SOFC electric power output that is auxiliary power to the aircraft relative to the total IPPS propulsive power ( $PW_{\text{IPPS}}$ ). All the terms in Eq. (28) can be written as a function of the  $PW_{\text{IPPS}}$  using the  $\text{DoH}_{\text{el}}$ , the electric powertrain efficiency, and either the SOFC electrical efficiency or the Ucell. This way, a normalized relationship, irrespective of size, is derived for the power potential (Eq. (29))

$$PP_{\text{el+th}} = \frac{PW_{\text{IPPS}} \cdot \left( \text{DoH}_{\text{el}} + R_{\text{NPP}} + \frac{\frac{\text{DoH}_{\text{el}}}{\eta_{\text{el,powertrain}}} + R_{\text{NPP}}}{\eta_{\text{SOFC}}} \cdot (1 - \eta_{\text{SOFC}}) \cdot \eta_{\text{HPT}} \cdot \eta_{\text{FT}} \right)}{\frac{PW_{\text{IPPS}} \cdot \left( \frac{\text{DoH}_{\text{el}}}{\eta_{\text{el,powertrain}}} + R_{\text{NPP}} \right)}{\eta_{\text{SOFC}}}} \quad (29)$$

**2.8 Modeling and Assumptions.** The gas turbine performance simulation is performed with Turbomatch, the Cranfield University in-house gas turbine performance software, which has been coupled with the SOFC enthalpy balance and mass balance 0D model developed by the Karlsruhe Institute of Technology (KIT) and described in Ref. [12]. Finally, anode and cathode heat exchanger thermodynamic balance relationships are applied to implement the GT-SOFC system interfaces as described in Appendix D.

Turbomatch is a component-based gas turbine simulation tool that resolves the thermodynamic cycle, the power balance, and mass continuity with steady-state design and off-design and transient analysis capability [13–15]. Gas turbine performance simulation methods are well-established in gas turbine performance theory textbooks [16,17] and will not be reiterated.

The SOFC used in this design exploration is agnostic to specific stack or cell technologies, so that its parameters Ucell, AU and FU can vary freely within acceptable ranges in order to represent different technology and sizing options. A minimum Ucell = 0.6V is a typical, conservative value that allows a safety margin against local fuel depletion and associated risk of nickel reoxidation. An upper bound of

Ucell = 0.9V was selected because for Ucell > 0.9V the resulting power density (in W/cm<sup>2</sup>) would be expected to be too low for aerospace applications. AU is primarily the cooling lever as the excess cathode air carries away the reaction heat and controls the temperature rise. The lower bound of AU = 0.1 was selected so that the cell performance is not negatively affected by excessive cooling while the higher bound of AU = 0.3 leaves sufficient margin from the physical, hard limit AU ~ 1 which would result in excessive stack overheating and mass transport limitations. Usually, the temperature gradient constraint of the SOFC materials is reached before the air depletion constraint, therefore, an upper bound of AU = 0.3 is reasonable for the present exploration. Overall, the selected ranges of Ucell and AU start from typical SOFC values today and extend up to values that could be expected with future technology but still allow sufficient safety margins from the physical limits of the system. The FU has been parameterized within a range of 0.1–0.9 in the aerothermal analysis of Sec. 3.3, although these are not hard limits, and FU up to 0.95 was accepted in the cycle potential investigation (Sec. 4). Overall, FU < 0.1 could lead to excessive stack cooling and values very close to 1 would cause fuel depletion which can damage the stack.

The Ucell, AU and FU are inputs to the mass balance and enthalpy balance model described in Ref. [12] to calculate the input and output species air flows and the SOFC outlet temperature. Based on the favorable design regions identified with the presented methodology, the SOFC stack development and sizing can be informed.

Five main modeling assumptions are declared:

- (1) In the cycles with bleed extraction, it is assumed that there is a small, negligible pressure drop through the SOFC system that is equal to or just lower than the pressure drops through the GT duct between the HPC and the combustor. The relative HEX/SOFC pressure drop and staying below the GT duct pressure drop becomes an objective of the detailed HEX and stack design, however, a 3–5% might be a realistic target. It is a physical constraint of the system that the SOFC gases must be introduced at a pressure equal, or higher than the reentry point in the GT gas path. The GT model implements a 5% overall pressure drop from the HPC outlet to the combustor outlet; however, the exact distribution of pressure losses through the HEX/SOFC system and staying below the GT duct loss becomes a subsystem design problem and is out of the scope of this thermodynamic exploration.
- (2) The temperature rise through the SOFC is calculated by resolving the enthalpy balance between the inlet and outlet species of the SOFC, while assuming the SOFC system is adiabatic. Heat transfer between the SOFC stack and the environment may reduce the temperature rise of the SOFC gases and, therefore, the enthalpy contribution to the gas turbine. The exact heat loss to the environment depends on the residence time and casing insulation. Although the heat loss to the environment is neither known nor within the scope of a conceptual-level analysis, it can become an objective of the detailed design to design a well-insulated SOFC stack housing that minimizes the heat loss to the environment.
- (3) Many modern GT performance software calculate the thermodynamic gas properties at each component as a function of the fuel-to-air ratio (FAR) and WAR for each fuel type, and do not support a feature for oxygen depleted air that is not a result of combustion. Similar functions of FAR are proposed by the well-known gas turbine performance textbook [16]. The in-house gas turbine performance tool Turbomatch operates on a similar principle, but before any intervention to its gas composition model, the numerical error due to this limitation will be assessed externally by quantifying the Cp as a function of individual species rather than FAR and WAR within the full range of possible oxygen fractions that can be met in this type of system. At BleedRatio = 1, for an AU of 0.4, which is far from SOFC operation today, the relative Cp error at the combustor inlet is 0.6%, and for AU of 0.1 the relative Cp error is 0.1%. With the addition of steam at the combustor inlet, this error becomes even more negligible due to the higher Cp imposed by the steam. This assessment can help prioritize the future development areas of gas turbine software that are able to simulate such GT-SOFC systems.
- (4) A constant cooling fraction of 5% will be assumed, and conservative isentropic efficiencies of 85% for the compressors and 87% for the turbines. This paper focuses on the thermodynamic analysis and potential of GT-SOFC engines over a range of cycles, and it is not a design study with specific materials or technology. A perspective on sizing, technology, and application considerations will be provided in Sec. 6.
- (5) The fuel system delivery temperature to the SOFC anode HEX cold side inlet and mixing plenum to supplement the additional required before the GT combustor, is assumed at 300K.

### 3 Results—Part A: Aerothermal Effects and Modeling

The overall range for the cycle potential investigation is  $TET = 1200\text{--}1800\text{ K}$ , which will be shown in Sec. 4. However, first, it is important that the aerothermal effects of the SOFC integration into the GT cycle are captured and analyzed. In this section, the aerothermal impacts are analyzed with reference to two cycles: (a) a lower specific power cycle ( $TET = 1300\text{ K}$ ) and (b) a higher specific power cycle ( $TET = 1700\text{ K}$ ).

**3.1 Impact of Oxygen Depletion.** When GT air goes through the SOFC, oxygen gets consumed by the reaction, therefore, the air at the combustor inlet, after the remixing of the cathode products, does not have the standard air composition. Looking at a wide range of  $DoH_{el}$ , AU and BleedRatio up to 1 (all air through the SOFC), the range of oxygen fraction at the combustor inlet can drop to  $\sim 16\%$  from the  $\sim 23.3\%$  standard oxygen fraction (Fig. 3). The minimum oxygen mass fraction is  $\sim 16\%$  irrespective of the TET and OPR, because it will always occur at the maximum possible  $DoH_{el}$  of the cycle for BleedRatio = 1. It only depends on the maximum acceptable AU and BleedRatio (Figs. 3(a) and 3(c)).

For a wide range of combustor inlet temperatures (representing different OPR and flight conditions) and oxygen mass fractions (based on Fig. 3), the change of Cp at the combustor inlet due to the oxygen depletion (before the addition of anode products) is demonstrated in Fig. 4. The change in Cp only due to the oxygen depletion is negligible, as it can be up to 0.6% (for an extreme AU of 0.4, which is very far from SOFC typical operation today) relative to the standard air composition with  $\sim 23.3\%$   $O_2$ . For BleedRatio = 1 and AU = 0.1 which is more typical for SOFCs, the Cp change drops to 0.1–0.2% compared to standard air composition. This can be attributed to the close Cp values between  $N_2$  and  $O_2$  (Fig. 29 in Appendix C), therefore, the small shift in their fraction does not have a significant thermodynamic impact. However, another concern that arises is the potential impact of the oxygen depletion on the combustion performance and mixing characteristics of the  $H_2$  with the  $O_2$ .

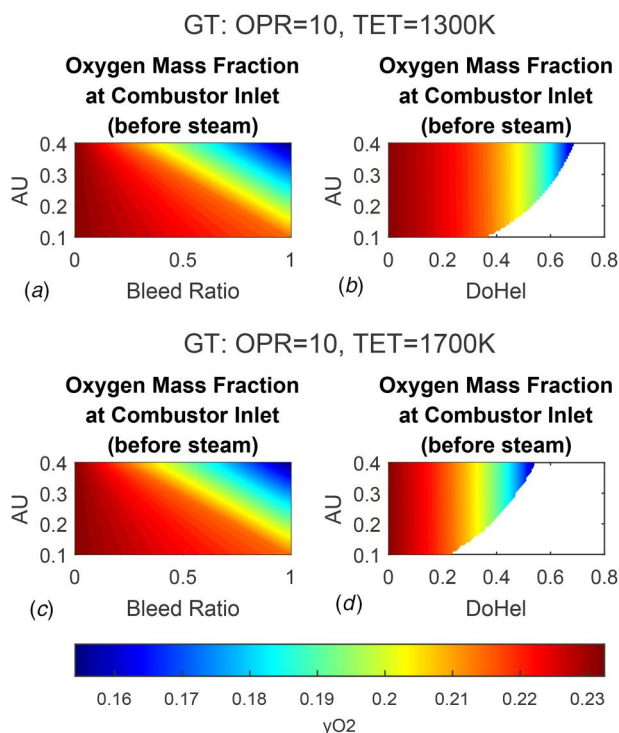


**Fig. 3** Oxygen fraction after the mixing of the GT flow with the cathode outlet products for Ucell = 0.6V

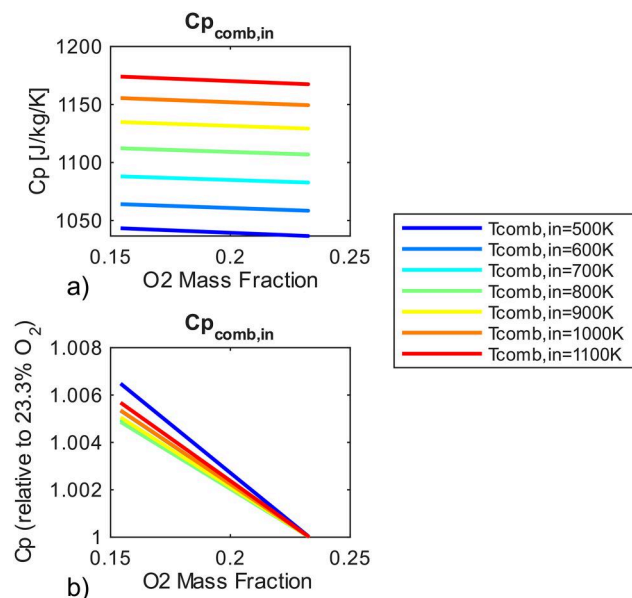


**Fig. 4** Change in combustor inlet  $C_p$  (before steam addition) due to the effect of oxygen depletion at 500–1100 K relative to standard dry air composition

**3.2 Impact of Water/Steam to Air Ratio.** The next effect that is analyzed is how the total WAR evolves at the combustor outlet due to SOFC steam and hydrogen combustion (Fig. 5), and how it is translated to higher specific heat capacity (Fig. 6) at different GT designs (TET) and combinations of SOFC Ucell and AU. The  $DoH_{el}$  where the lines stop is where the airflow availability limit is approached (BleedRatio  $\sim 1$ ) for each combination of TET, AU, and Ucell.

Although an increase in WAR alone at the combustor may have been expected to increase the FAR to achieve the same TET with higher steam content, it is concluded that the increase in combustion inlet temperature (Fig. 7), which decreases the required  $dT$  through the combustor due to the mixing of SOFC cathode gas with the GT main gas path dominates the tradeoff and reduces the FAR at higher  $DoH_{el}$  and higher Ucell with the gradient being accelerated toward the higher  $DoH_{el}$  (Fig. 8). Overall, there is an exponential increase in WAR (Fig. 5) because moving from a lower to higher  $DoH_{el}$ , the produced  $H_2O$  increases and the GT air flow reduces at the same time. Similarly, there is an exponential increase in combustor inlet temperature (Fig. 7) because the bleed ratio also increases nonlinearly as the SOFC air flow increases and the GT air flow reduces at the same time. The rate of increase in combustor inlet temperature is further accelerated at lower Ucell (Fig. 7).

When examining a set  $DoH_{el}$  and AU, the lower Ucell results in higher cathode outlet temperature. The AU affects the cathode outlet temperature but also bleed ratio, thus the mixing ratio and mixing temperature of the cathode outlet with the GT main flow (Fig. 7). The design TET also affects the mixing ratio as for a constant GT target power output, it changes the specific power, thus the GT air flow. The lower specific power GT (TET = 1300K) always has lower mixing temperature compared to higher specific power GT (TET = 1700K) at a set  $DoH_{el}$ , AU, and Ucell. However, in all cases, the maximum  $T_{comb,in}$  is the same and only a function of the AU and Ucell (and OPR which is constant in this figure), but it appears at different  $DoH_{el}$ , when the BleedRatio = 1.

Considering the tradeoff between SOFC steam and combustion production WAR, the change in the combustor outlet  $C_p$  is calculated relative to the baseline  $H_2$ GT of the same TET, and the maximum increase was  $\sim 6\%$ .

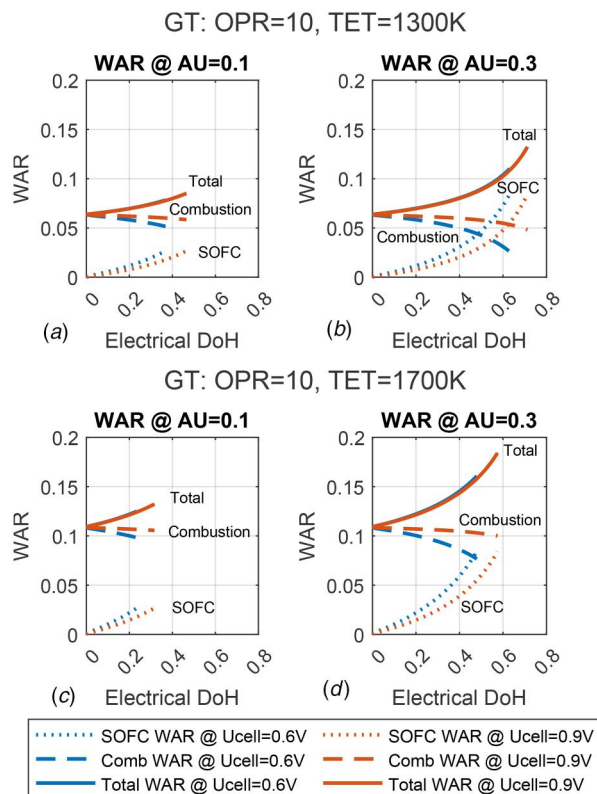


**Fig. 5** WAR change due to SOFC steam, GT combustion and total WAR

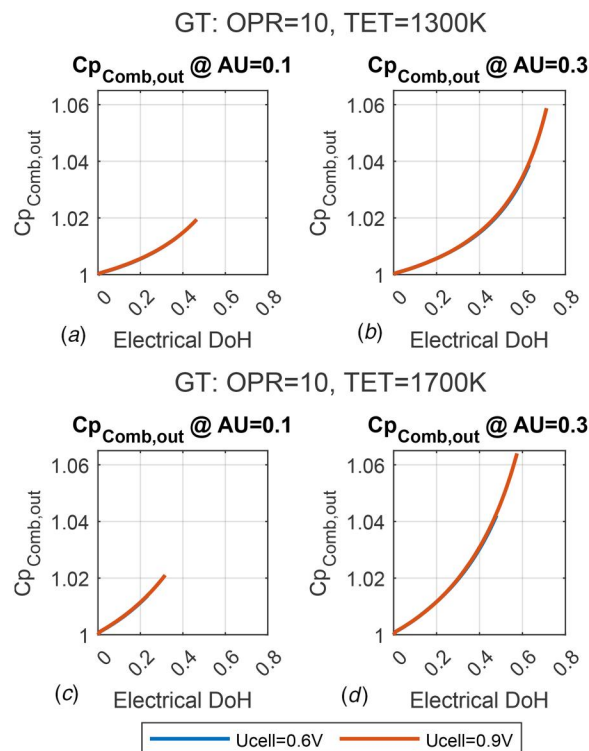


**Fig. 6** Change in combustor outlet  $C_p$  due to WAR relative to the baseline  $H_2$  GT

**3.3 Impact of Anode Products and Solid Oxide Fuel Cell Fuel Utilization.** A reduction in FU increases the unutilized hydrogen; therefore, less fuel must be supplemented by the cooler

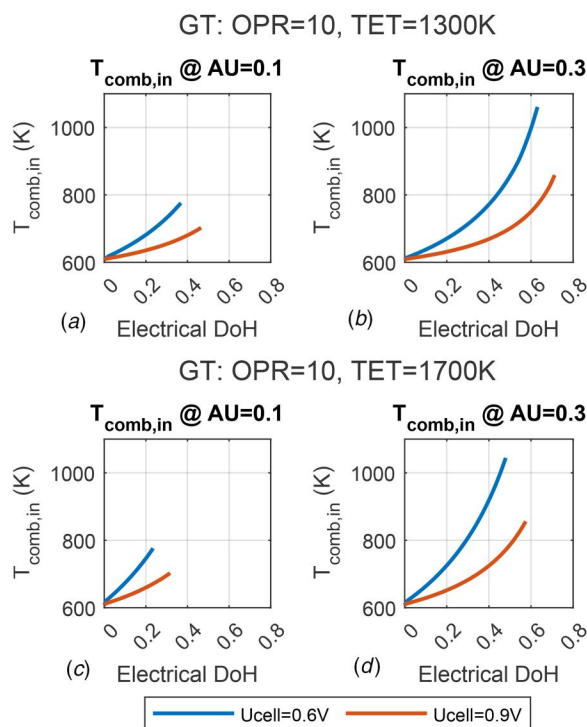


Fig. 7 Change in combustor inlet temperature

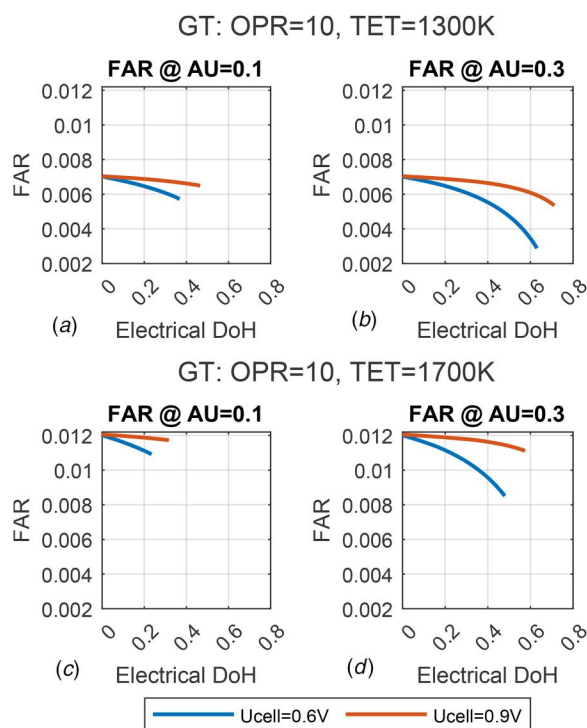


Fig. 8 FAR change as a function of the  $DoH_{el}$  at different combinations of TET, Ucell, and AU

fuel system for GT combustion. When the  $H_2Ratio > 1$  (Fig. 9), either the FU should increase or the  $DoH_{el}$  should decrease. The point of crossing the  $H_2Ratio = 1$  is also affected by the GT design TET as it affects the baseline fuel flow which starts being replaced by hydrogen power to the SOFC. In Fig. 9, the case of TET = 1300K is presented, but at higher TETs the  $H_2Ratio = 1$  point starts shifting to the right toward higher FU as less total  $H_2$  would be required by

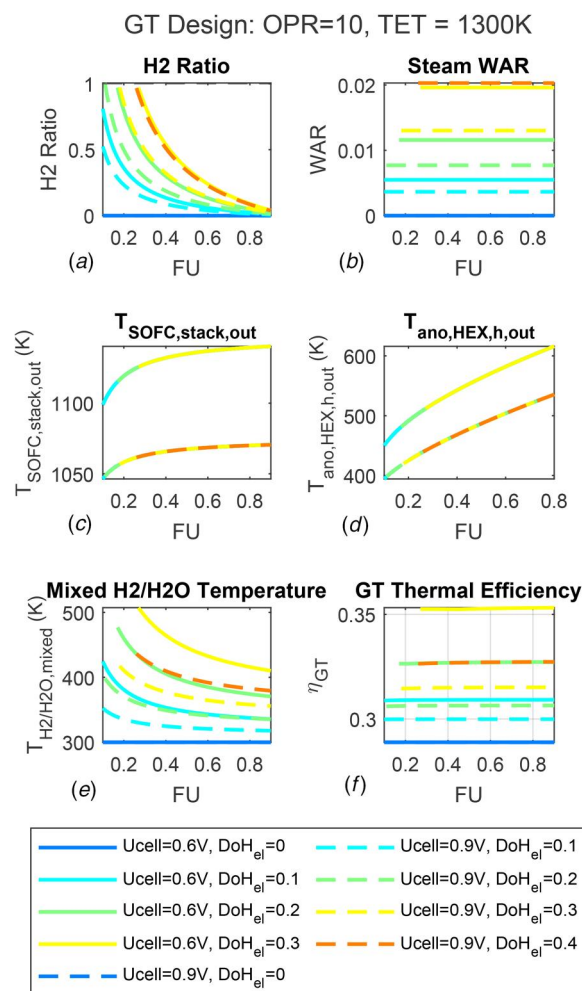


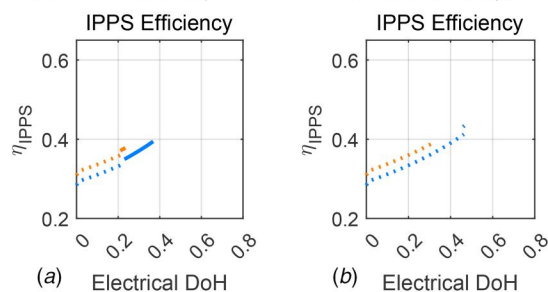
Fig. 9 Effect of FU on GT integration (a)  $H_2$  Ratio, (b) steam WAR (due to SOFC), (c) SOFC stack outlet temperature, (d) anode HEX hot side outlet, (e) mixed  $H_2/H_2O$  temperature, and (f) gas turbine thermal efficiency (when the  $H_2O$  is dry steam)

the more efficient GT cycle. The combinations of lower FU and higher  $DoH_{el}$  that exceed the  $H_2Ratio = 1$  line result in excess unutilized hydrogen to the GT and have been filtered out. These design combinations exceeding  $H_2Ratio = 1$  must be rejected from the design space, unless a hydrogen venting or recirculation system is added at the outlet of the SOFC system.

At FU as low as 0.1, there is a cooling effect on the stack (Fig. 9(c)) due to the high flow of excess hydrogen. The combinations of Ucell and  $DoH_{el}$  that start at  $H_2Ratio = 1$  (Fig. 9(a)) have a high  $H_2/H_2O$  mixed temperature equal to the  $T_{ano,HEX,h,out}$  (Figs. 9(d) and 9(e)). This is because no  $H_2$  must be supplemented from the fuel system, so the mixed temperature in reality follows the anode HEX hot side outlet temperature (Fig. 9(d)). Once the FU has entered the feasible range with  $H_2Ratio \leq 1$  for the given Ucell and  $DoH_{el}$  (Fig. 9(a)), the rate of decrease in  $H_2Ratio$  (i.e., ratio of unutilized SOFC  $H_2$  to total GT  $H_2$ ) seems to overcompensate for the small stack heating effect that any further increase in FU causes (Fig. 9(c)), therefore, the  $T_{H_2/H_2O,mixed}$  reduces with increasing FU (Fig. 9(e)). In terms of the resulting steam WAR (Fig. 9(b)), this curve remains flat as each line represents a given  $DoH_{el}$  and Ucell, and the produced water is only a function of the consumed hydrogen and Ucell and is unaffected by the excess hydrogen.

**3.4 Steam Condensation Conditions and Impact of  $H_2O$  Phase Change.** Through this thermodynamic exploration, design space regions were identified with possibility for SOFC steam

SOFC: Ucell=0.6V, AU =0.1 SOFC: Ucell=0.9V, AU =0.1



SOFC: Ucell=0.6V, AU =0.3 SOFC: Ucell=0.9V, AU =0.3

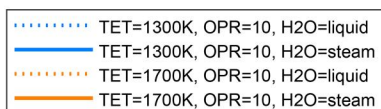
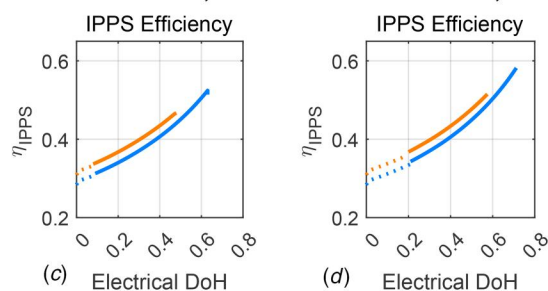


**Fig. 10 Crossing of saturation point and impact of H<sub>2</sub>O phase change extremes on overall efficiency**

condensation (liquid or wet steam) at the H<sub>2</sub>/H<sub>2</sub>O mixing plenum due to the mixing of the anode products with the cooler hydrogen coming from the fuel system. Figure 10 compares the extreme cases where all H<sub>2</sub>O is dry steam and where all H<sub>2</sub>O is in the liquid phase, with the jump indicating the transition point when the saturation point is crossed.

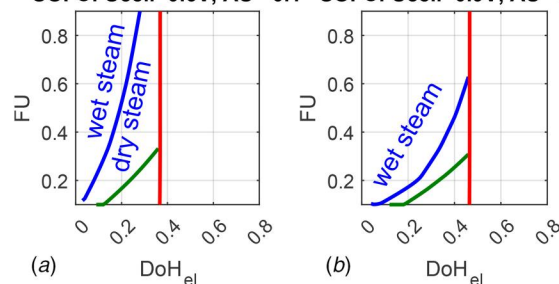
Liquid water makes the cycle up to 2% less efficient than dry steam within the investigated range (Fig. 10) but has higher Cp and thus higher cooling potential to reduce the flame temperature and NO<sub>x</sub>. In reality, as the DoH<sub>el</sub> decreases or the FU increases, there will be a gradual transition to wet steam with larger liquid fractions. In this paper, the presence of this effect is presented for the first time and the impact of the two extreme phases on the overall efficiency is assessed. In future designs, if a region with possible condensation is selected, development of two-phase H<sub>2</sub>O combustor models will be needed and potentially two-phase anode HEX models.

For a given U<sub>cell</sub> and DoH<sub>el</sub>, the phase change from steam to liquid happens with increasing FU because less of the hotter SOFC residual H<sub>2</sub> is allowed and more of the cooler fuel system H<sub>2</sub> is supplemented, increasing the possibility of condensation (Fig. 11). In terms of DoH<sub>el</sub> impact, higher DoH<sub>el</sub> reduces the possibility of condensation and moves the phase change to higher FU (Fig. 11) as there is more residual H<sub>2</sub>, therefore the H<sub>2</sub>/H<sub>2</sub>O remains hotter than the saturation point up to a higher FU. A higher U<sub>cell</sub> or lower AU reduce the FU where phase change starts, because the SOFC outlet temperature is cooler, therefore, the mixing temperature is cooler, too.

The impact of condensation on efficiency becomes more noticeable at higher DoH<sub>el</sub> and higher specific GT power cycles because both increase the WAR (Fig. 10). If the fuel system delivery temperature was increased, the phase change from wet steam to superheated steam would move toward lower DoH<sub>el</sub> and higher FU. Finally, less efficient gas turbine cycles, i.e., low specific power cycles, require more fuel system H<sub>2</sub> than high specific power cycles under the same AU, U<sub>cell</sub> and DoH<sub>el</sub>, thus the saturation line is crossed at higher DoH<sub>el</sub> or lower FU. Between cycles optimized for OPR and cycles constrained at OPR = 10, the cycles with the

GT Design: TET=1300K, OPR =10

SOFC: Ucell=0.6V, AU =0.1 SOFC: Ucell=0.9V, AU =0.1



SOFC: Ucell=0.6V, AU =0.3 SOFC: Ucell=0.9V, AU =0.3

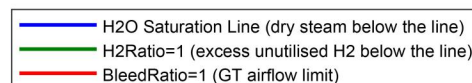
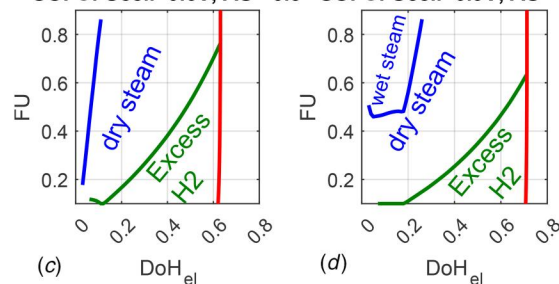


**Fig. 11 H<sub>2</sub>O saturation line of H<sub>2</sub>O/H<sub>2</sub>O mixture as a function of FU and DoH<sub>el</sub>**

optimized, higher OPR are expected to move the condensation region up to slightly higher DoH<sub>el</sub> due to the higher operating pressure and thus higher H<sub>2</sub>O partial pressure, which reduces the saturation temperature.

**3.5 Impact of Overall Pressure Ratio on Cathode Heat Exchanger.** The cathode HEX is sized so that the cold side outlet matches the target SOFC stack inlet temperature of 973 K (Figs. 12(c)–12(e)). Since the stack inlet temperature is constant, the outlet temperature is also constant at a given U<sub>cell</sub> and AU. The engine OPR affects the HPC outlet temperature, which is the cathode HEX cold side inlet, and thus has a direct impact on the required design point cathode HEX effectiveness to meet the required target SOFC stack inlet temperature (Fig. 12).

An increase in OPR was found beneficial in three ways:

- It improves the baseline gas turbine thermal efficiency (before any thermodynamic coupling) as shown in Fig. 32 and the hybrid GT efficiency (Fig. 12(a)) as long as it is not combined with a very low TET where a high OPR can start becoming penalizing. At TET = 1300 K, the OPR increase is beneficial up to ~22 (Fig. 12(a)). Although increasing OPR toward the optimal OPR improves the baseline cycle, it increases the specific power of the GT, thus reducing its max DoH<sub>el</sub> potential.
- It increases the compressor outlet temperature, thus, increasing the SOFC system inlet temperature (Figs. 12(d) and 12(e)) and reducing the required HEX effectiveness (Fig. 12(c)) and size of the HEX when designing the HEX for a target SOFC stack inlet temperature at design point.
- At a set dT through the SOFC stack, a higher OPR is beneficial as it increases the HEX cold side inlet temperature, thus reducing the heat duty of the cathode HEX and the required temperature drop at the HEX hot side to preheat the incoming SOFC air to the stack. This way, higher temperature cathode air can be remixed with the main gas path, which increases the combustor inlet temperature and reduces the need for fuel combustion.

GT Design: TET=1300K  
SOFC Design: AU=0.1

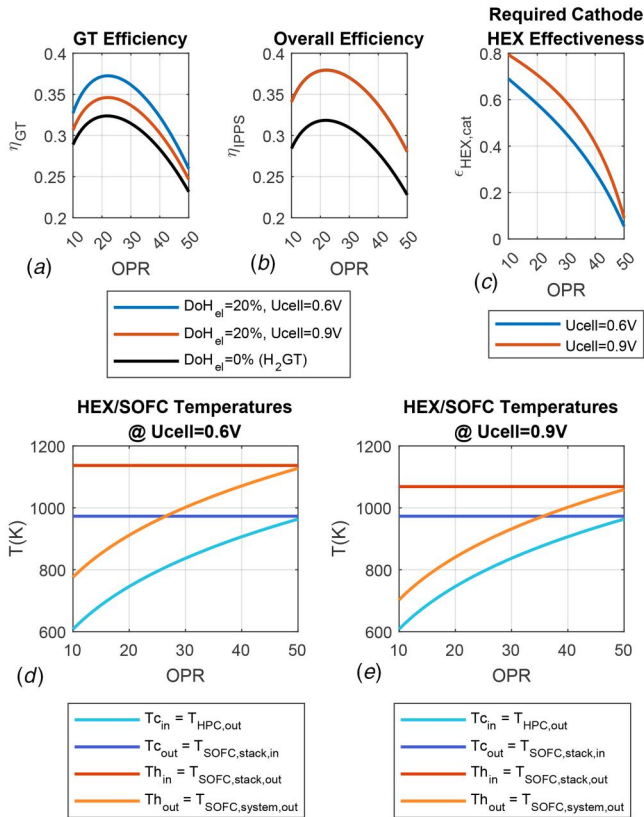


**Fig. 12** Impact of OPR and Ucell on GT thermal efficiency, overall effective and cathode HEX target effectiveness at  $\text{DoH}_{el}=20\%$  and  $\text{TET} = 1300 \text{ K}$

It should be noted that, at a given ambient condition, the required HEX effectiveness and the hot and cold side temperatures are solely prescribed by the OPR, the SOFC stack outlet temperature, and the target SOFC stack inlet temperature. Therefore, the selection of  $\text{TET} = 1300 \text{ K}$  as the case for demonstration does not influence the observations on the HEX performance (Figs. 12(c)–12(e)). However, the OPR impact on GT efficiency and overall efficiency (Figs. 12(a) and 12(b)) are specific to the TET, and Sec. 4 will cover more combinations of TET/OPR.

Another consideration is that there is a diminishing rate of return in the improvement of SOFC performance with increasing pressure from 5 atm to 10 atm [18,19]. At very high pressures, there is risk for gas leakage and the use of heavy pressure vessels will be needed. Combined with the fact that turboshaft gas turbine efficiency improves more with TET while OPR increase gives a small rise in efficiency, an integration strategy could be to constrain the gas turbine OPR to 10. However, due to the benefit coming from reducing the cathode HEX hot side temperature drop at high OPR, a tradeoff analysis with the SOFC mass implication is needed. It should be noted that this positive impact of OPR is specific to the examined architecture, where the stack outlet gas performs the additional preheating of the SOFC inlet air. In an alternative HEX position where the hot side is fed by the free turbine outlet, there might be a limit in the maximum OPR that is beneficial, as it reduces the exhaust gas temperature (EGT) for a given TET. However, in such an architecture, there might be an optimum tradeoff between increasing HPC outlet temperature and reducing the EGT.

**3.6 Thermodynamic Versus Electrical Hybridization.** The thermodynamic coupling of the GT-SOFC opens up a new design

space with additional degrees-of-freedom compared to the “conventional” hybridization strategies with electric power only. In this section, it is demonstrated how different combinations of subsystem design and technology parameters lead to different thermodynamic degrees of hybridization at the same electrical degree of hybridization.

At a set  $\text{DoH}_{el}$ , a lower Ucell increases the GT thermal efficiency (Figs. 14(a) and 14(d)) as there is a higher temperature rise through the SOFC (Figs. 13(c) and 13(f)) and a higher BleedRatio (Figs. 13(b) and 13(e)). This means that more air is diverted and heated up through the SOFC to a higher temperature. At a set  $\text{DoH}_{el}$  and Ucell, a lower AU also means higher BleedRatio to the SOFC (Figs. 13(b) and 13(e)), but the  $\Delta T$  through the SOFC stack becomes lower (Figs. 13(c) and 13(f)).

There is an interesting tradeoff in terms of Ucell. At the lower  $\text{DoH}_{el}$ , the relative enthalpy contribution of the SOFC is low as its power and possible generated heat are also low; therefore, they offer a modest improvement to the GT thermal efficiency. At the higher  $\text{DoH}_{el}$ , the relative contribution of the SOFC to the GT, the  $\text{DoH}_{thermo}$  (Figs. 13(a) and 13(d)) and GT thermal efficiency (Figs. 14(a) and 14(d)) are further boosted, but since the GT power has been downsized and becomes the minority power contribution to the IPPS, the overall IPPS efficiency is dominated by the SOFC electrical efficiency and is less affected by the now excellent GT efficiency ( $>40\%$ ) (Figs. 14(a) and 14(d)). Considering the tradeoff between the GT thermal efficiency (Figs. 14(a) and 14(c)) and the SOFC electrical efficiency (Figs. 14(b) and 14(e)) when changing the Ucell, the curves of overall efficiency nearly coincide (Figs. 14(c) and 14(f)) irrespective of the Ucell-AU combination, but what differs is the maximum achievable  $\text{DoH}_{el}$  with each combination Ucell/AU, which is determined by when the BleedRatio reaches one (Figs. 13(b) and 13(e)).

In regards with the impact of the design TET, although the baseline GT thermal efficiency starts slightly lower when designing for lower TET, the maximum possible thermal efficiency is higher within the investigated range of SOFC design parameters (Figs. 14(a) and 14(d)). The maximum thermal efficiency was 82.3% for  $\text{AU} = 0.3$  and  $\text{Ucell} = 0.6 \text{ V}$  at  $\text{DoH}_{el} = 63\%$  and  $\text{TET} = 1300 \text{ K}$  (Fig. 14(a)). The limit of BleedRatio = 1 is reached at higher  $\text{DoH}_{el}$  for the lower TET of 1300 K (Figs. 13(b) and 13(e)), which has lower specific power in terms of  $\text{kW}/(\text{kg}/\text{s})$  and therefore the maximum  $\text{DoH}_{el}$  and overall IPPS efficiency were also observed for  $\text{TET} = 1300 \text{ K}$ . The maximum observed overall efficiency is 58.2% at  $\text{DoH}_{el} \approx 71\%$  for  $\text{TET} = 1300 \text{ K}$ ,  $\text{AU} = 0.3$  and  $\text{Ucell} = 0.9 \text{ V}$  (Fig. 14(c)).

## 4 Results—Part B: Coupled Aerothermal Performance Exploration and Cycle Potential

**4.1 Cycle Potential.** A cycle analysis with  $\text{TET} = 1200\text{--}1800 \text{ K}$  was performed:

- (1) with the OPR constrained at 10, assuming the SOFC will not be able to accommodate higher pressures than 10 atm or higher-pressure ratios than 10 relative to the freestream.
- (2) with the optimum OPR of the cycle within an investigated range of  $\text{OPR} = 10\text{--}50$ .

For each cycle, the  $\text{DoH}_{el}$  was increased until the limit BleedRatio = 1 or  $H_2\text{Ratio} = 1$  was reached without further margin for increasing the FU. At the same time, the SOFC parameters were varied between  $\text{AU} = 0.1\text{--}0.3$  and  $\text{Ucell} = 0.6\text{--}0.9 \text{ V}$ . The different cycle options and SOFC design parameter combinations are compared in terms of overall efficiency, efficiency change, absolute SFC, and SFC relative to the baseline  $H_2\text{GT}$  with  $\text{DoH} = 0\%$ .

In cycles with high  $\text{DoH}_{el}$  and  $\text{AU} = 0.3$  resulting in high  $T_{cat,HEX,out}$  and hot  $T_{H_2}/H_2O,mixed}$ , thus reducing the total  $H_2$  needed by the GT combustor, the  $H_2\text{Ratio} = 1$  constraint can be reached first. However, there was margin to increase the FU from 0.6 in steps of 0.01 until the FU reaches 0.95 (allowing a safety margin from 1). Supplementary figures monitoring the evolution of the constrained

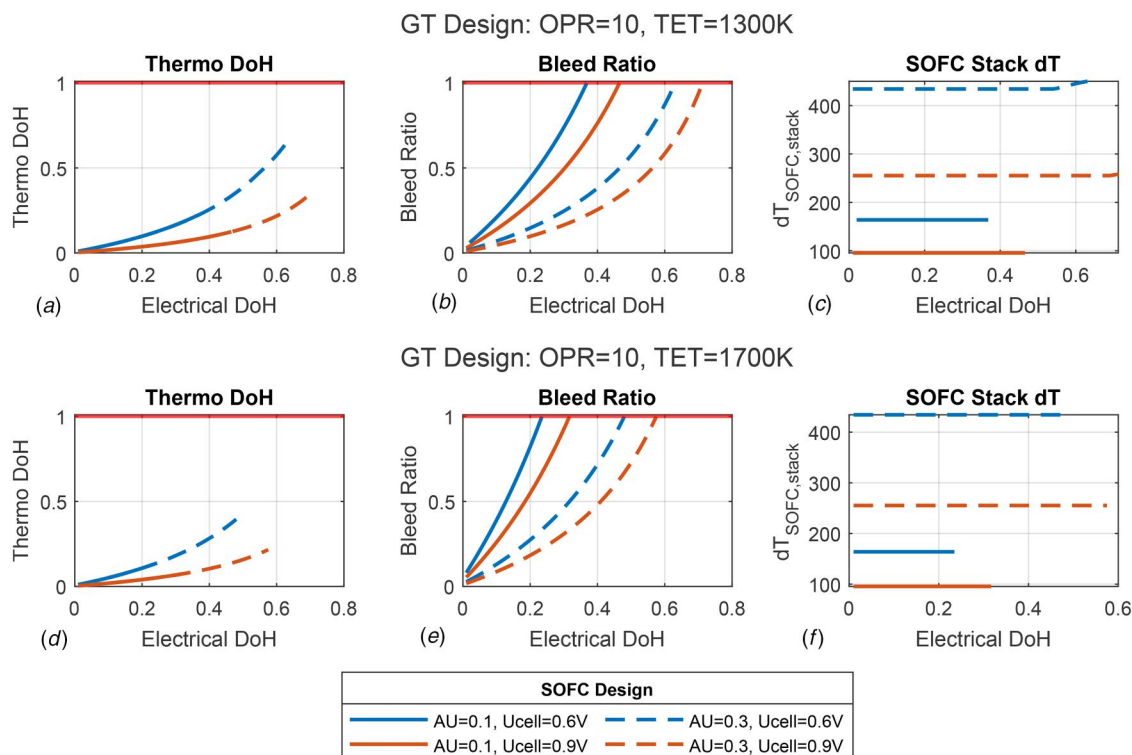


Fig. 13 Thermodynamic hybridization versus electrical hybridization

parameters and adaptation of FU (Figs. 41 and 37) are provided in Appendix E (Figs. 33–42).

The low specific power cycles, although their baseline ( $DoH_{el} = 0\%$ ) efficiency is lower (Figs. 15 and 16), therefore, their baseline SFC is higher, the maximum relative benefit they can get from SOFC integration is higher, as the higher airflow availability

enables higher maximum  $DoH_{el}$  (Figs. 17 and 18). In terms of relative benefits, the low-specific-power cycles have a relative SFC reduction up to  $-55.7\%$  at their optimum OPR (Fig. 18) and up to  $-55.8\%$  when the OPR is constrained at 10, when  $U_{cell} = 0.9V$  and  $AU = 0.3$  (Fig. 17(d)). High specific power cycles can have a relative SFC reduction up to  $-30\%$  at their optimum OPR (Fig. 18)

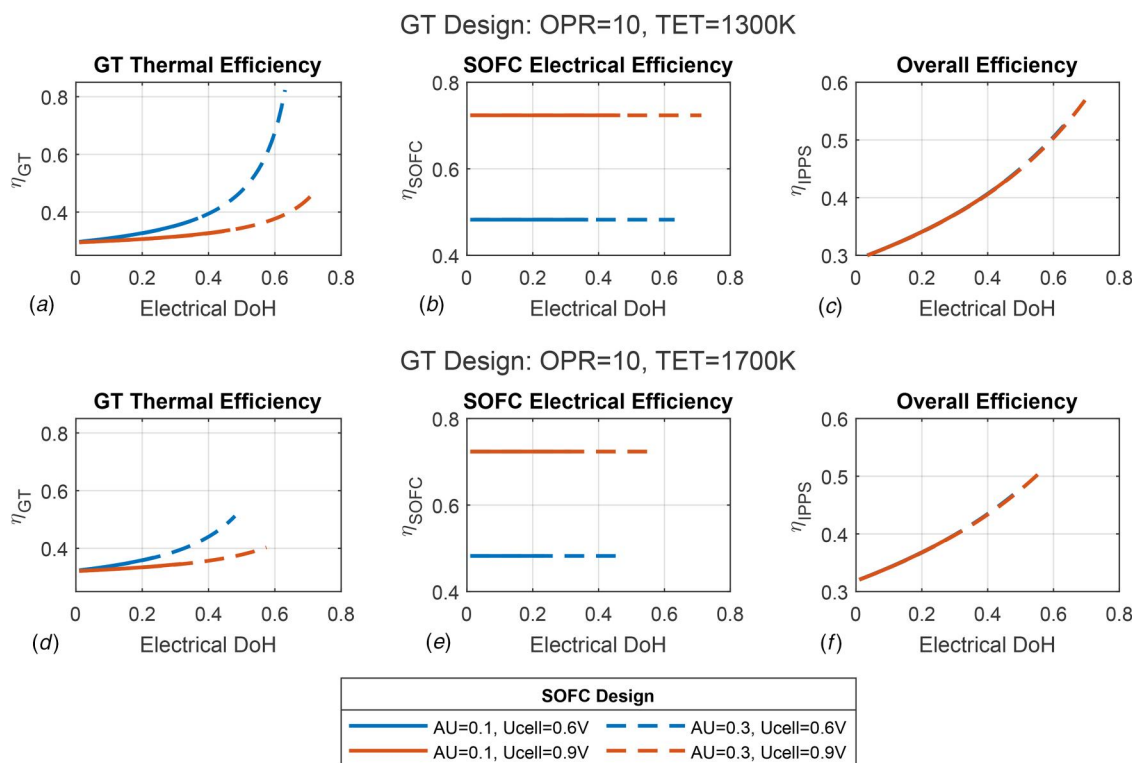


Fig. 14 GT, SOFC, and overall efficiency with variable  $U_{cell}$ - $AU$  combinations when designing for a target  $DoH_{el}$

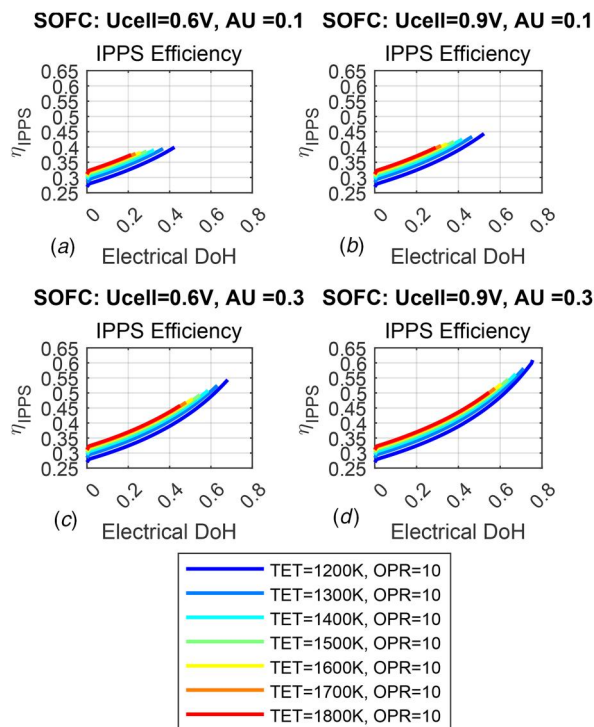


**Fig. 15** IPPS overall efficiency for different cycles when the OPR is constrained at 10 for (a)  $U_{cell}=0.6V$  and  $AU=0.1$ , (b)  $U_{cell}=0.9V$  and  $AU=0.1$ , (c)  $U_{cell}=0.6V$  and  $AU=0.3$ , and (d)  $U_{cell}=0.9V$  and  $AU=0.3$

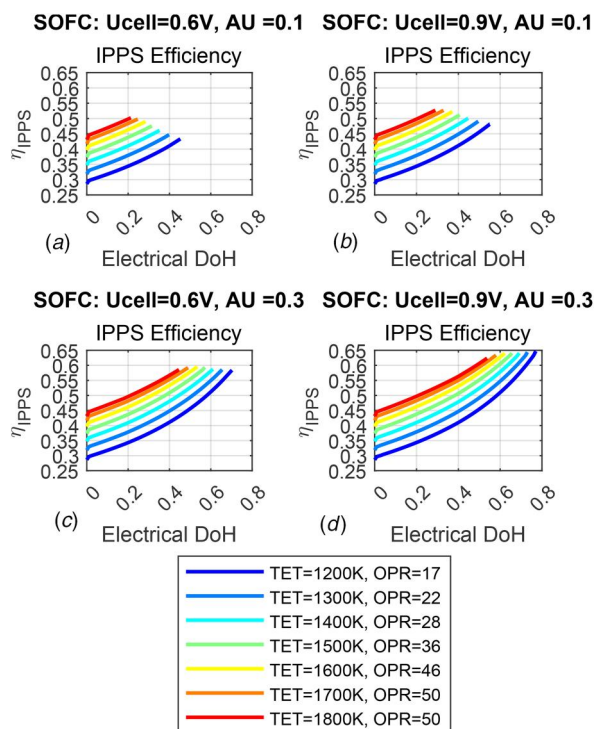


**Fig. 16** IPPS overall efficiency for different cycles when the OPR is optimum for the TET for (a)  $U_{cell}=0.6V$  and  $AU=0.1$ , (b)  $U_{cell}=0.9V$  and  $AU=0.1$ , (c)  $U_{cell}=0.6V$  and  $AU=0.3$ , and (d)  $U_{cell}=0.9V$  and  $AU=0.3$

and up to  $-37.9\%$  when the OPR is constrained at 10 due to the SOFC technology (Fig. 17(d)).

However, it can be argued that the SFC and efficiency improvement should also be quoted against the  $H_2GT$  cycle of the same TET

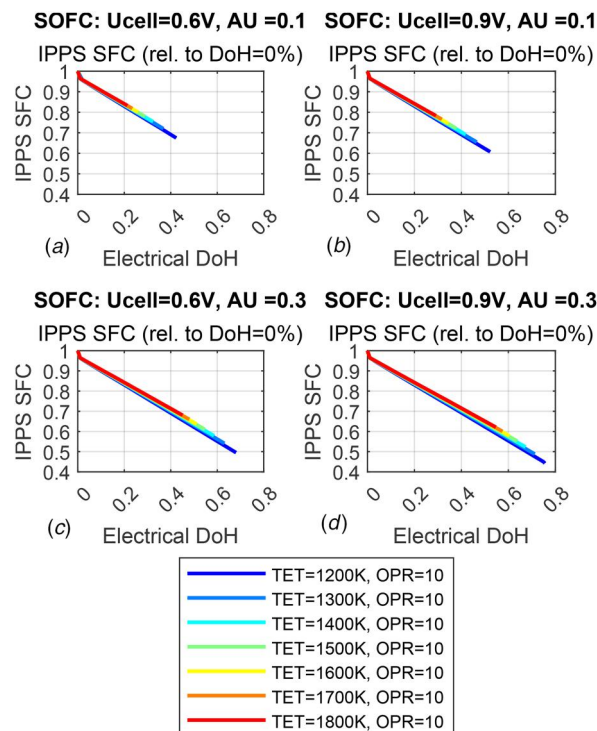


**Fig. 17** Combined SFC relative to  $DoH=0\%$  for different cycles when the OPR is constrained at 10 for (a)  $U_{cell}=0.6V$  and  $AU=0.1$ , (b)  $U_{cell}=0.9V$  and  $AU=0.1$ , (c)  $U_{cell}=0.6V$  and  $AU=0.3$ , and (d)  $U_{cell}=0.9V$  and  $AU=0.3$

but optimized OPR. The  $H_2GT$  does not have the SOFC constraint, however, constraining the OPR to lower values than the optimum can be used as a strategy to reduce  $NO_x$  emissions or due to sizing constraints. Therefore, the benchmark should be carefully selected depending on the reference application. For completeness, using Figs. 33 and 34 with absolute SFC in Appendix E, the comparison between GT-SOFC cycles with constrained OPR and  $H_2GT$  cycles with their optimum OPR can also be made. The lowest specific power GT-SOFC cycle (1200 K) constrained at OPR = 10 with the most advanced SOFC scenario ( $AU=0.3$  and  $U_{cell}=0.9V$ ) can get a maximum benefit of  $-53\%$  SFC reduction compared to the  $H_2GT$  cycle (i.e.,  $DoH_{el}=0\%$  in Fig. 33) with TET = 1200K/OPR = 17. The highest specific power GT-SOFC cycle (1800 K) constrained at OPR = 10 with the most advanced SOFC scenario ( $AU=0.3$  and  $U_{cell}=0.9V$ ) can get a maximum benefit of  $-14.1\%$  SFC reduction compared to the  $H_2GT$  cycle (i.e.,  $DoH_{el}=0\%$  in Fig. 33) with TET = 1800 K/OPR = 50. The higher specific power GT-SOFC cycles are more impacted by the constraint OPR = 10 as their optimum OPR is much higher, and they can even be penalized and have worse performance than the  $H_2GT$  cycle with  $DoH=0\%$  when the  $AU$  is close to today's value, i.e.,  $AU=0.1$ . A higher  $AU=0.3$  seems to be able to compensate for the penalty of constraining the OPR in high TET cycles.

When the OPR is constrained at 10, in all SOFC scenarios, the low specific power cycles (low TET) achieve higher maximum overall efficiency than high specific power cycles (Fig. 15), while for optimized OPR, the high specific power cycles (high TET) have a higher absolute efficiency than low specific power cycles (Fig. 16). Only for the most SOFC advanced scenario ( $U_{cell}=0.9V$  and  $AU=0.3$ ) in Fig. 16(d), the low specific power cycles with optimized OPR can break even the absolute efficiency of the high specific power cycles with optimized OPR.

When the OPR is optimized for the cycle's TET, then in most cases, the maximum overall efficiency remains higher for the high specific power/high baseline efficiency cycles (Figs. 16(a)–16(c)). The end of each line in Figs. 15–20 is where the BleedRatio = 1

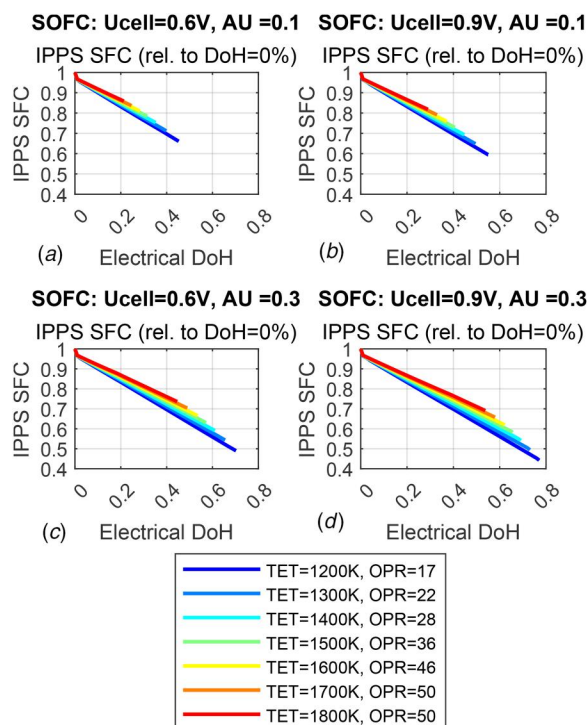


**Fig. 18** Combined SFC relative to DoH = 0% for different cycles when the OPR is optimum for the TET for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

limit was reached. The low cycle/low baseline efficiency cycles can only catch up and reach similar overall efficiency only if the Ucell = 0.9V and AU = 0.3 (Fig. 16(d)). However, when the OPR is constrained at 10 (Fig. 15), in all cases, the low specific power cycles

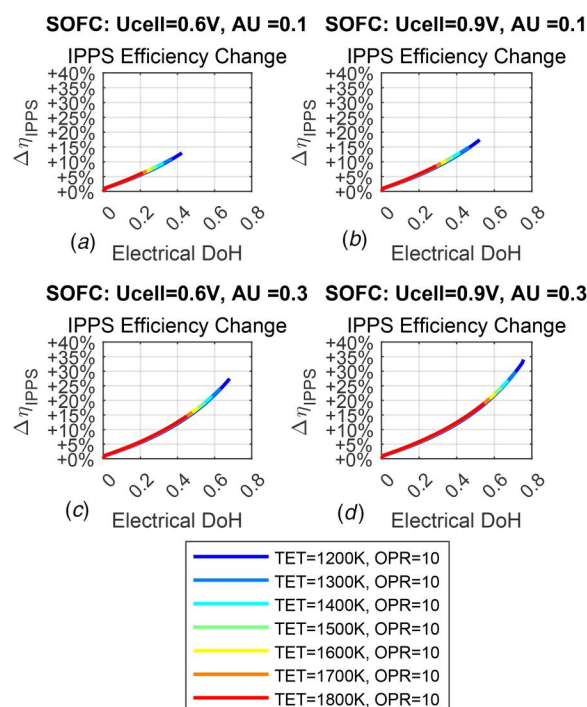


**Fig. 19** IPPS overall efficiency change from DoH = 0% for different cycles when the OPR is constrained at 10 for (a) Ucell = 0.6V and AU = 0.1 (b) Ucell = 0.9V and AU = 0.1 (c) Ucell = 0.6V and AU = 0.3 (d) Ucell = 0.9V and AU = 0.3

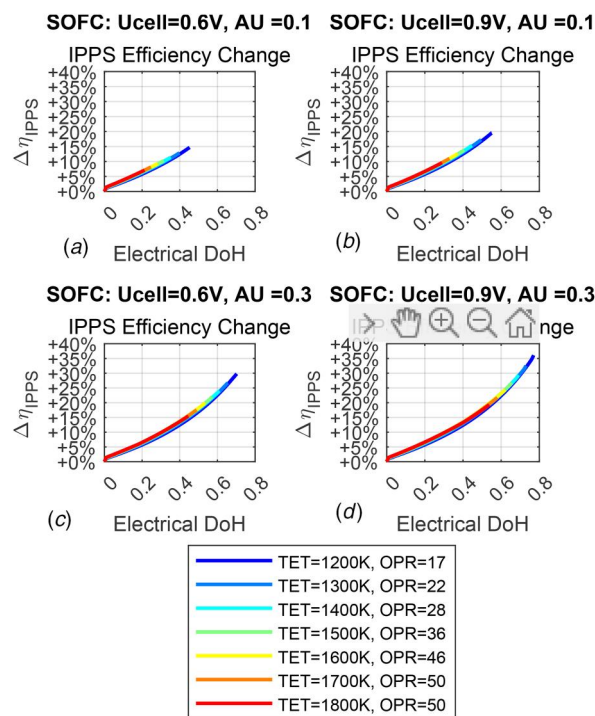


**Fig. 20** IPPS overall efficiency change from DoH = 0% for different cycles when the OPR is optimum for the TET for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

offer the maximum overall efficiency, as the high specific power cycles are more penalized by the OPR constraint. Finally, it is observed that irrespective of the cycle and SOFC parameters, the efficiency change is a unique function of the  $DoH_{el}$  (Figs. 19 and 20), but what changes is the max  $DoH_{el}$  that can be achieved for each cycle and SOFC parameter combination. It is also observed that an increase in AU from 0.1 to 0.3 is more impactful in increasing the  $DoH_{el}$  than an increase in Ucell from 0.6V to 0.9V, explained through the impact on decreasing the requested SOFC air for a given SOFC power (Eq. (A1)).

When looking at the efficiency change in terms of  $(\eta_{IPPS} - \eta_{GT, baseline(DoH=0\%)})$ , the most interesting observation was that, irrespective of the cycle, the lines of efficiency change vs  $DoH_{el}$  nearly coincide for all cycles and combinations of Ucell/AU (Figs. 19 and 20). However, the maximum  $DoH_{el}$  and maximum efficiency change that each cycle can achieve is prescribed by the specific power of the cycle and the SOFC Ucell and AU, which both affect the air flow of the SOFC, with the AU having a stronger impact.

**4.2 Generic Gas Turbine-Solid Oxide Fuel Cell Design Maps.** In the previous section, the efficiency change due to the SOFC integration was observed to be a strong function of the  $DoH_{el}$ , with very little influence from the Ucell and gas turbine specific power (TET/OPR). However, the combination of air flow availability of the GT and the air flow demand of the SOFC determine the maximum possible  $DoH_{el}$  (for BleedRatio = 1) and, therefore, maximum efficiency benefit. This gave the inspiration for the creation of universal coupling maps.

Figure 21 is a universal design map for the coupling of the GT with the SOFC. Figure 21(a) provides the observed unique relationship between  $DoH_{el}$  and absolute efficiency change. Figure 21(b) provides the max  $DoH_{el}$  that can be achieved for different combinations of GT and SOFC specific powers, which represents the airflow availability to feed the SOFC. Finally, the thermal efficiency and specific power of the GT can be mapped out to the TET/OPR combination from the GT design map in Fig. 21(c). At the

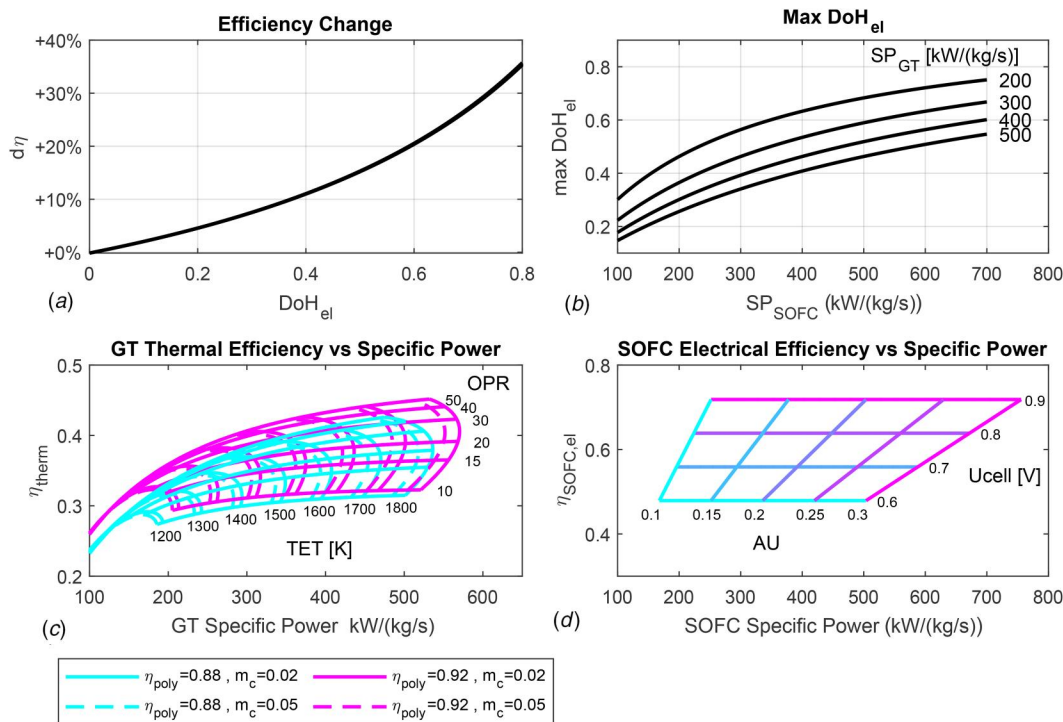


**Fig. 21 Coupled design maps for GT-SOFC system (a) Efficiency change potential based on  $DoH_{el}$  (b) Maximum  $DoH_{el}$  as a function of the GT and SOFC specific powers (c) Gas turbine carpet plots with thermal efficiency and specific power as a function of the OPR and TET (d) SOFC carpet plot with electrical efficiency and specific power as a function of the AU and  $U_{cell}$**

same time, in Fig. 21(c), each of TET/OPR carpet plot is presented for four combinations of cooling fraction and compressor polytropic efficiency to represent different technologies, materials, and sizes. Similarly, the electrical efficiency and specific power of the SOFC can be found from the AU/ $U_{cell}$  combinations in the SOFC design map in Fig. 21(d).

This nearly unique function of the efficiency change to the  $DoH_{el}$  is explained and approximated with the proposed analytical methods in Sec. 2.7.2. This trend is also verified from the full gas turbine performance simulations, as the efficiency change lines of the different cycles nearly coincide (Figs. 19 and 20). The very small offsets from the mean efficiency change line are attributed to three main factors:

- The smaller core gas turbines (i.e., higher TET, combined with optimum OPR) have higher steam WAR for the same steam flow and same  $DoH_{el}$ , therefore, their benefit is slightly higher than the low specific power cycles
- For higher OPR cycles, the reacting  $O_2$  has a higher temperature, therefore, higher enthalpy  $H_2O$  is directly added to the cycle.
- Phase change of the  $H_2O$  happening at different  $DoH_{el}$  based on the combination  $U_{cell}$ , AU, FU (affecting the anode HEX outlet temperature, and unused hydrogen) and the TET, OPR (affecting the additional  $H_2$  that must be supplemented by the cooler fuel system  $H_2$ ). Different  $H_2O$  phase between the curves causes an estimated deviation of up to  $\sim 2\%$ . The universal GT-SOFC design maps assume vapor, but the method can be quickly adapted to predict the efficiency change if the phase of the  $H_2O$  is known.

The aforementioned effects explain why the higher TET cycles with optimum OPR have higher offset from the mean efficiency change line than the higher TET cycle with OPR constrained at 10. However, the above effects are secondary and the overall efficiency change due to  $DoH_{el}$  can be adequately explained and represented analytically using the methods described in Secs. 2.7.2 and 2.7.3.

The overall efficiency change lines for the different  $U_{cell}$  and different cycles nearly coincide because there is a potential for the SOFC to produce either electric motor power output to the propeller, auxiliary power to the aircraft or GT shaft power through the enthalpy contribution to the cycle by exploiting around 87–91% of its input fuel power (Fig. 22) as estimated using Eq. (29). The contribution of the SOFC to the overall efficiency equation is fractional and out of this fraction, no matter how efficiently or inefficiently the electrical power is produced, there is potential to recover around 87–91% of the SOFC consumed fuel power either as electrical power or GT shaft power to the propeller.

**4.3 Designing for a Constant  $DoH_{el}$  Versus Constant Bleed Ratio.** When designing the GT-SOFC system for a target  $DoH_{el}$ , the maximum air flow availability might not be exploited, and a uniform, vertical offset is observed for each  $DoH_{el}$  carpet plot in

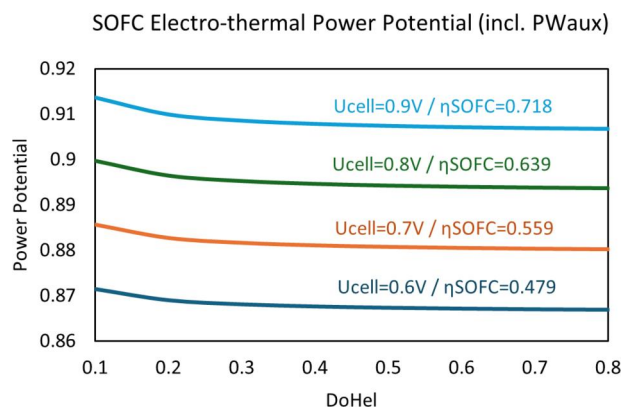


**Fig. 22 Potential of the SOFC to produce electrical power or shaft power through its thermal power**

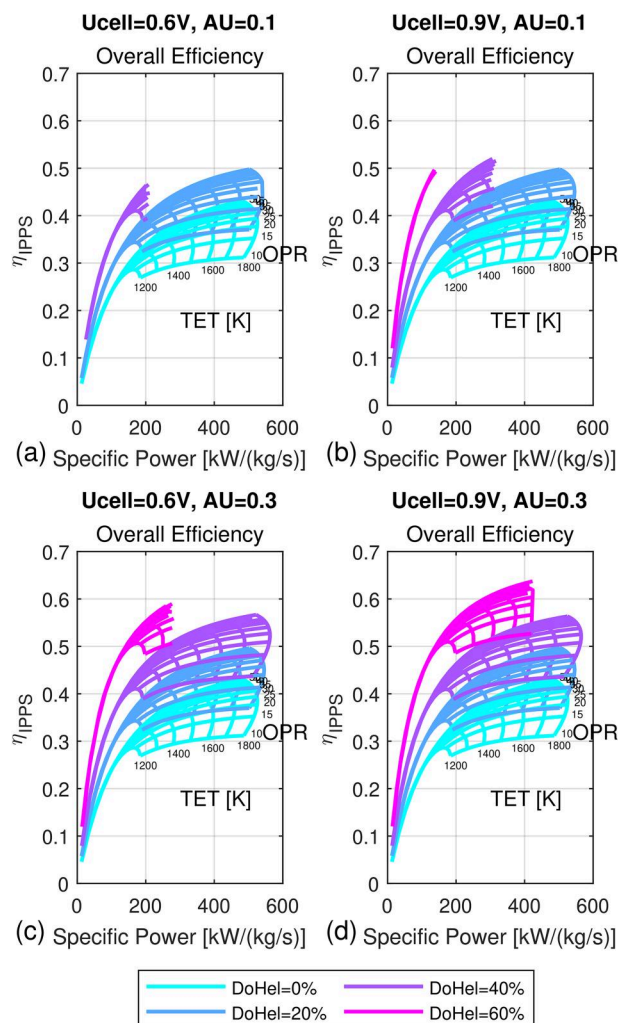


**Fig. 23 Gas turbine design point carpet plots for DoHel = 0–20–40–60% and (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3**

relation to the baseline GT carpet plot (Fig. 23). This is in line with the previous observation that the efficiency change is a nearly unique function of the  $DoH_{el}$ . A very small horizontal offset can also be observed with increasing  $DoH_{el}$  (Fig. 23), and this is attributed to the higher WAR at the combustor outlet (Fig. 5). This increase in WAR slightly boosts the specific power of the hybrid cycles, although the TET remains the primary driver.

However, when designing for a constant BleedRatio = 1, the carpet plot shifts upward across the entire range but with a larger increase on the left side (low TETs) than on the right side (high TETs), resulting in an apparent rotation of the carpet plot while the SOFC is progressing from Ucell = 0.6V and AU = 0.1 (Fig. 24(a)) to Ucell = 0.9V and AU = 0.3V (Fig. 24(d)). This happens because the low specific power cycles start achieving a sufficiently higher  $DoH_{el}$  than low specific power cycles, which can overcompensate for the lower, baseline efficiency (Fig. 24 DoHel = 0%). The crossover between the carpet plot for DoHel = 0% and BleedRatio = 0 in Figs. 24(a) and 24(b) reveals that:

- For AU = 0.1 and Ucell = 0.6V, if the OPR of the high specific power GT-SOFC cycles with TET > 1500 K is constrained at 10, they cannot breakeven with the performance of the OPR-optimized GT-only cycles of same TET.
- For AU = 0.1 and Ucell = 0.9V, if the OPR of the high specific power GT-SOFC cycles with TET > 1600 K is constrained at

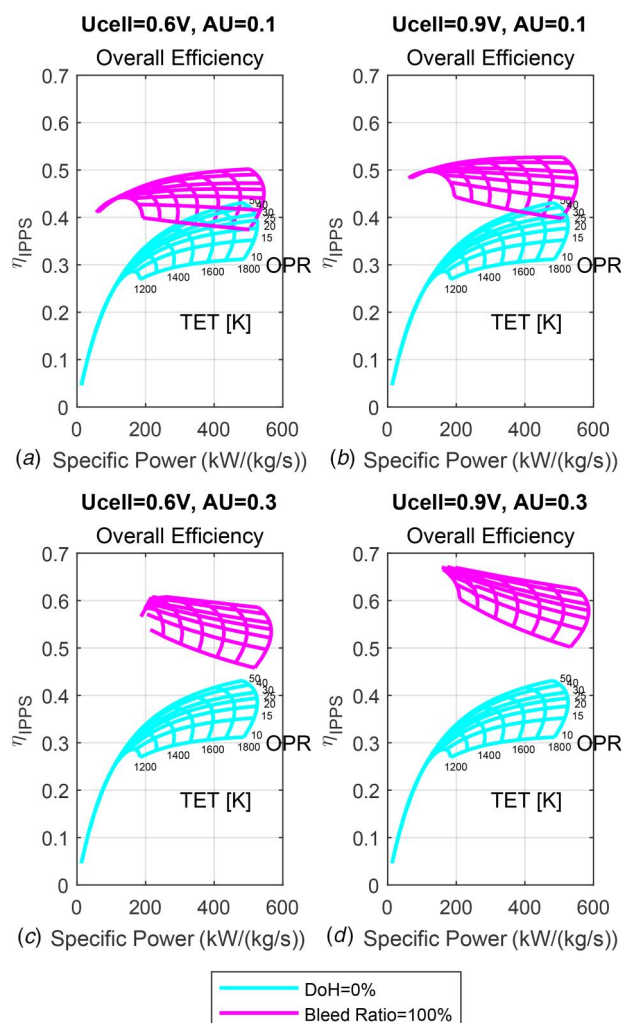


**Fig. 24 Gas turbine design point carpet plots for DoH = 0% and BleedRatio = 100% and (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3**

10, they cannot breakeven with the performance of the OPR-optimized GT-only cycles of same TET.

Only for high AU = 0.3, the GT-SOFC high specific power cycle can get an efficiency improvement with constrained OPR = 10 compared to nonhybrid cycles of same TET but optimized OPR (Figs. 24(c) and 24(d)). For TET = 1800K, this efficiency improvement is up to +8% with constrained OPR against the OPR-optimized GT-only cycle. Overall, at AU = 0.1, the lower specific power cycles are benefited from SOFC integration irrespective of whether the OPR can be optimized or not, while the higher specific power GT-only cycles have better efficiency with optimized OPR than with an SOFC that limits their OPR to 10 (Figs. 24(a) and 24(b)).

## 5 Discussion on NO<sub>x</sub> Emission Impacts

The analysis demonstrated that the SOFC integration increases the combustor inlet air temperature (Fig. 7) and H<sub>2</sub> temperature (Fig. 9), which both improve the gas turbine efficiency but can increase the NO<sub>x</sub> emissions by increasing the flame temperature [20,21]. On the other side, the SOFC steam (Fig. 5) is expected to have a cooling effect on the flame temperature and reduce NO<sub>x</sub> [22,23] with liquid H<sub>2</sub>O being expected to have a stronger cooling effect. The reduction in FAR (Fig. 8) due to the replacement of combustion heat release with SOFC enthalpy contribution, together with the electrification of

**Table 1 Expected impact of different SOFC integration effects on NO<sub>x</sub> emissions (qualitatively)**

| SOFC integration effect          | Expected impact on NO <sub>x</sub> |
|----------------------------------|------------------------------------|
| ↓ GT power                       | ↓↓ (stronger decrease)             |
| ↓ FAR                            | ↓ (decrease)                       |
| ↑ Fuel temperature               | ↑ (increase)                       |
| ↑ Combustor inlet temperature    | ↑ (increase)                       |
| ↑ WAR                            | ↓ (decrease)                       |
| If H <sub>2</sub> O is liquid    | ↓↓ (stronger decrease)             |
| If H <sub>2</sub> O is dry steam | ↓ (decrease)                       |
| If OPR is constrained            | ↓ (decrease)                       |
| If OPR can be optimized          | ↑ (increase)                       |

power (i.e., reducing the power produced by combustion), is expected to reduce the overall NO<sub>x</sub> emissions. However, the exact balances of the above competing parameters in reducing NO<sub>x</sub> (summarized in Table 1), the optimum tradeoffs between NO<sub>x</sub> and GT thermal efficiency and the detailed H<sub>2</sub>/H<sub>2</sub>O injection strategies should be experimentally investigated in the future.

## 6 Perspective on Sizing and Technology Considerations

The focus of this paper is the analysis of cycle thermodynamic impacts and cycle potential from a thermodynamic perspective, and in this section, additional sizing and technology considerations are introduced.

**6.1 Solid Oxide Fuel Cell.** It is important to note that the preceding cycle analysis treats the SOFC as a zero-dimensional enthalpy and charge balance, without resolving the electrochemical losses that govern the actual cell voltage at a given current density. The identified optimum at U<sub>cell</sub> = 0.9V and AU = 0.3 represents the thermodynamic optimum of the GT–SOFC coupling rather than a system-level optimum that accounts for stack sizing, mass, and technology constraints. However, it provides a direction for the future SOFC development. The following discussion examines to what extent this operating window is compatible with current and emerging SOFC technology, and where the actual system optimum is likely to shift.

An increasing U<sub>cell</sub> requires a higher total cell count ( $N_{\text{cells}}$ ) and cell area ( $A_{\text{cell}}$ ) to deliver the required SOFC power at the correspondingly lower current density ( $j$ ) and area-specific power density (PD) (Eq. (30) and Fig. 25), which directly increases stack mass. In conventional planar stacks, the metallic interconnects (MICs) can account for up to 70 % of the total stack mass [24]. Therefore, commercial stacks currently achieve gravimetric power densities typically around 0.1 kW/kg [25], while mobile applications require specific power densities in excess of 3–4 kW/kg [24],

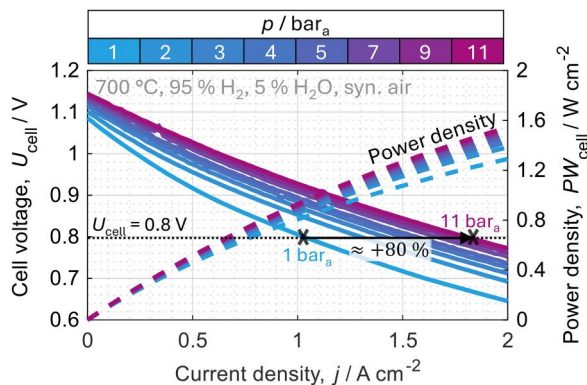


**Fig. 25 Effect of operating pressure on polarization and power density of a 1 cm<sup>2</sup> fuel electrode supported planar cell, measured on the pressurized test bench described in Ref. [19]**

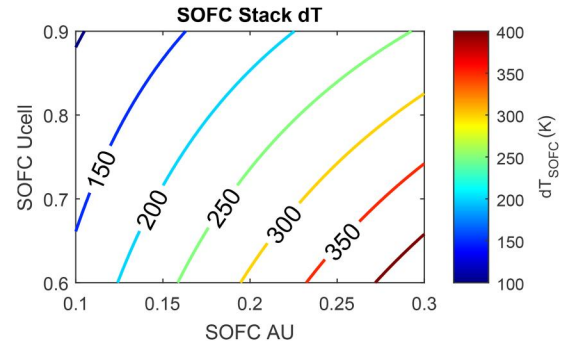


**Fig. 26 Temperature rise through the SOFC as a function of the AU and U<sub>cell</sub> for a constant FU = 0.6**

indicating that novel stack concepts are essential to bring the sizing into an acceptable range for aviation

$$PW_{\text{SOFC}} = N_{\text{cells}} \cdot j \cdot U_{\text{cell}} \cdot A_{\text{cell}} = N_{\text{cells}} \cdot PD \cdot A_{\text{cell}} \quad (30)$$

The operating temperature range is primarily dictated by the cell design—electrolyte-supported cells (ESCs) require temperatures typically above 800 °C to overcome the ohmic resistance of the thick electrolyte, whereas fuel-electrode-supported cells with thin electrolytes in the order of a few microns can operate at significantly lower temperatures of 600–750 °C while maintaining acceptable area-specific resistance. Within this window, higher temperatures reduce activation and ohmic overpotentials and thus increase the attainable cell voltage at a given current density but are bounded from above by accelerating degradation that compromises long-term performance and lifetime [26]. The resulting stack dT strongly depends on the remaining operating parameters such as AU. Typical planar SOFC stacks tolerate a maximum dT of 100–150 K [27], whereas the favorable operating point identified here requires 250–300 K (Fig. 26), significantly exceeding current design limits. Even though a further increase in AU is beneficial as it unlocks higher DoH<sub>el</sub>, it proportionally increases the heat generation and can lead to stack overheating if the internal heat generation exceeds the cooling capacity of the cathode air stream (Fig. 25). It must be noted that tubular concepts might be able to sustain even larger temperature gradients, which will be discussed in more detail at the end of this section.

In conventional stationary SOFC systems, a high stack FU (typically around 80–85%) is mandatory, as the remaining fuel must be converted in a downstream afterburner and should therefore be kept minimal. In the hydrogen SOFC-GT hybrid, this constraint is relaxed: Nonconverted hydrogen is fully combusted in the GT combustion chamber, recovering its chemical energy as thermal power. However, a lower bound on the FU exists, since the unused hydrogen from the SOFC must not exceed the total hydrogen supply to the GT. Therefore, the H<sub>2</sub>Ratio must remain below unity to avoid excess uncombusted hydrogen. Within this constraint, FUs well below 50% might be acceptable. This enables operating points with higher power densities due to the reduced gas conversion impact on the Nernst voltage, which shows one distinctive feature of the SOFC-GT architecture.

Overall, the aviation-integrated SOFC must meet requirements that go significantly beyond stationary practice: (i) more than an order-of-magnitude improvement in gravimetric power density, (ii) tolerance of a dT approximately twice the current limit, and (iii) thermal shock resilience for flight-phase transients. While the target U<sub>cell</sub> and AU are individually consistent with stationary operation, their combination with this mass and dT constraints creates a design space not addressed by current technology.

Microtubular SOFCs ( $\mu$ t-SOFCs) may offer a pathway to relax several of these constraints. Their small characteristic dimension and radial symmetry provide superior thermal shock resistance and avoid the massive MICs that dominate the planar stack weight. Since

$\mu$ t-SOFCs are physically separate units sharing a common gas environment, they can be freely arranged in parallel or series at the bundle level, offering electrical sizing flexibility not available in the MIC-enforced series topology of planar stacks. However, current collection from small-diameter tubes introduces substantial in-plane ohmic losses that scale with the tube length, which—together with limited packing efficiency—constrains the achievable volumetric and areal power density well below planar designs. A promising approach to mitigate these losses is the cell networking concept, in which multiple short, electrochemically active segments are deposited along a single tube and connected in series via conductive interconnect bands. Each segment has a short in-plane current path, reducing ohmic losses, while the segment voltages add up to a high single-tube voltage. On the bundle level, multiple segmented tubes are then connected in parallel to scale the total current. Although this concept could in principle distribute the thermal load across many small, individually robust cells and thus accommodate the dT regime identified here, practical demonstration at the required power density and scale remains an open research question [24,28–30].

Operating pressure constitutes an additional lever for cell performance. Pressurization, as inherently provided by the GT compressor, enhances the open-circuit voltage via the Nernst equation and improves electrode kinetics [31], shifting the polarization curve to higher current densities at a given cell voltage, as can be seen in Fig. 26 from 1 to 11 bara at 700 °C for a 1 cm<sup>2</sup> fuel electrode supported planar cell, measured on a recently developed pressurized single cell test bench described in Ref. [19]. Here, a performance increase up to 80% could be observed from 1 to 11 bara at 0.8 V. Importantly, these gains are limited to the pressure-dependent loss mechanisms—the ohmic resistance of the electrolyte and contact interfaces remains essentially pressure-invariant. The magnitude of the pressure-induced performance improvement is therefore strongly dependent on the relative contribution of the individual loss mechanisms: cells with high ohmic losses relative to polarization losses, for instance, due to a thick electrolyte or high contact resistances, will benefit significantly less from pressurization than cells in which activation and diffusion losses prevail, as is typically the case for thin-electrolyte, fuel-electrode-supported cells. At a given target  $U_{\text{cell}}$ , pressurization allows operation at higher current densities and, thus, partially alleviates the sizing penalty. However, pressurized operation requires a gas-tight housing or pressure vessel, which adds structural mass—in the PNNL conceptual design for a Boeing 787 SOFC power unit, the pressure vessel contributed approximately 15% of the total system mass alongside 62% from the stacks [32]. In addition, Fig. 26 shows that the pressure-dependent performance gain follows a logarithmic relationship. Beyond a certain operating pressure, further increases therefore yield only marginal improvements. From a system-level perspective, higher pressures further incur a gravimetric power density penalty since the pressure vessel wall thickness, and thus its mass must scale accordingly. The inherent pressurization in a GT-coupled architecture therefore remains beneficial from both a thermodynamic and cell-performance perspective, but the associated mass penalty underscores that the true system-level optimum in  $U_{\text{cell}}$  and pressure will inevitably differ from the thermodynamic optimum identified in the cycle analysis.

Ultimately, the preceding considerations suggest that the thermodynamically optimal operating point at  $U_{\text{cell}} = 0.9$  V and  $AU = 0.3$  may not be realizable at an acceptable gravimetric power density with foreseeable SOFC technology. Once the stack mass can no longer be reduced through architectural improvements alone—whether by eliminating metallic interconnects or adopting micro-tubular geometries with a networking approach, the remaining degrees-of-freedom are the operating conditions themselves. Of these, increasing  $AU$  emerges as the dominant lever: higher air utilization directly unlocks greater degrees of hybridization and higher IPPS efficiency, and should therefore be treated as a design priority.  $U_{\text{cell}}$ , by contrast, should most likely be kept low to avoid the mass penalty of additional cells, though sufficient voltage margin must be preserved to accommodate stack degradation over

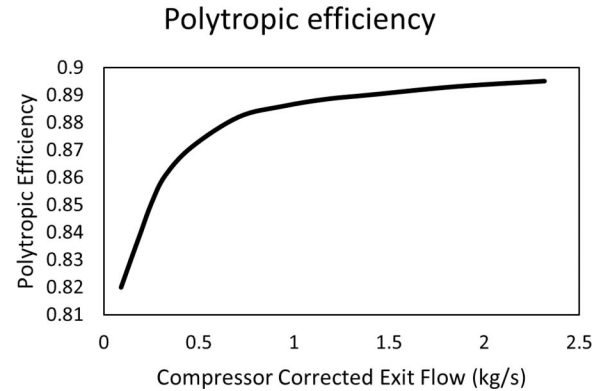


Fig. 27 Polytropic efficiency against compressor corrected exit flow (adapted from Ref. [36])

the intended times on wing. Reducing  $U_{\text{cell}}$  to 0.7 V at high  $AU$  yields a substantially higher power density and a lower stack mass but produces steep axial temperature gradients ( $\Delta T > 300$  K along the flow channel). These can be mitigated through  $\mu$ -tubular cell geometries with single-ended mounting, which permit free thermal expansion and prevent the build-up of in-plane stress critical in planar stacks, and by increasing cell length to distribute the gradient over a greater axial distance. The elevated outlet temperature nevertheless remains an open challenge in terms of degradation, one that targeted material selection may help address. The true system-level optimum therefore emerges from the tradeoff between the efficiency benefit of high  $AU$  and low  $U_{\text{cell}}$ , which reduces fuel consumption and stack mass, and the long-term stability constraints that bound both—a balance that can only be resolved through a coupled analysis integrating the real CV-characteristic, the stack mass model, and the aircraft-level mission fuel burn, a step beyond the thermodynamic exploration presented here, but one for which the preceding analysis provides the necessary foundation.

**6.2 Gas Turbine.** In terms of gas turbine sizing, it was found that lower specific cycles with low TET can reach higher  $DoH_{el}$ , particularly if combined with high  $AU$ . Considering that low specific power cycles are typically used in smaller aero-engine applications (i.e., commuter and regional aircraft) that are good candidates for hybridization, caution is needed. A high  $DoH_{el}$  results in downsizing of the gas turbine core which leads to lower turbomachinery efficiencies, mainly in the compressors, due to lower Reynolds number [32–33] and increase in tip clearance relative to the blade height [35]. In conceptual gas turbine design, correlations of the polytropic efficiency with blade height or mass flow are used to represent the efficiency deterioration due to tip clearance effects and low Mach number. An example is given, adapted from NASA's work [36] in Fig. 27.

For the coupling regions with  $BleedRatio < 1$ , implementing variable, large fractions of bleed/bypass within the gas path to feed the SOFC is extremely challenging. Although it seems attractive theoretically, as with a smaller, lighter SOFC, the  $DoH_{el}$  could be increased at cruise with a larger bleed ratio [37], this would not only require large and heavy actuators, but it would also cause upstream flow distortion and compressor instability [38,39].

In terms of benchmarking, the results include baseline  $H_2GT$  performance, i.e.,  $DoH_{el} = 0\%$  for TET = 1200–1800 K combined with both optimized OPR and constrained OPR = 10 (Figs. 15–20 and Figs. 23 and 24). Therefore, it could be debated whether the OPR-optimized or OPR-constrained GT-only cycle should be the benchmark. In conventional GTs, when the size allows it, the usual design choice is to optimize the OPR for the TET. For advanced, larger GT applications, the design evolution moves toward higher TET and higher, optimized OPR.

If the reference application is a small gas turbine (less than a couple of MWs) with sizing constraints and cost-reduction objectives, the

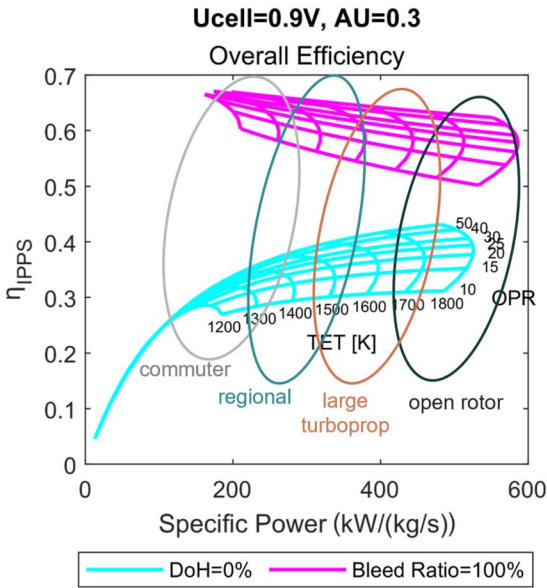


**Fig. 28** GT-SOFC maximum potential for turboshaft aero-engines

benchmark should probably have a TET in the lower range, combined with a nonoptimal, constrained OPR. If the reference application is an advanced, larger GT with no sizing constraints, it might be more appropriate to compare the performance of OPR-constrained GT-SOFC cycles against OPR-optimized, GT-only cycles, since it can be argued that if the SOFC is not present, the OPR constraint is not needed. Finally, another consideration is that in  $H_2$  GTs, where a small efficiency boost can be expected from the  $H_2$  adoption and the  $CO_2$  emissions are already eliminated, limiting the TET and OPR could be potential strategies for mitigating  $NO_x$ . Therefore, depending on the application and design objectives, the reader should refer to the appropriate benchmark to identify the true benefit that can come from the SOFC integration.

## 7 Aero-Engine Turboshaft Applications

In this section, the maximum potential of different turboshaft applications is discussed, assuming an optimistic scenario of SOFC parameters at  $U_{cell}=0.9V$  and  $AU=0.3$ . Low specific power cycles for commuter (e.g., Beechcraft 1900D, Textron Inc., Providence, RI) and regional aircraft (e.g., ATR72, ATR, Blagnac, France) have the largest potential both in terms of relative improvement and maximum combined efficiency, if the OPR is constrained at 10. Larger turboshaft applications such as large turboprops for transport applications (e.g., A400M Atlas, Airbus SE, Toulouse, France) and future open rotor engines (e.g., RISE, CFM International, Cincinnati, OH), although they have a smaller margin for further improvement, they can still benefit significantly from SOFC integration. Even if the SOFC technology is constrained at  $OPR=10$ , the high specific power cycles can still achieve combined GT-SOFC efficiencies up to 50–55% with  $AU=0.3$ , while with an optimized OPR they can reach efficiencies up to 60–62% (Fig. 28). Considering that very high specific power cycles are already close to their limitation in terms of materials, thermodynamics and improvement margin, the potential +8% absolute efficiency change between the baseline OPR-optimized  $H_2$ GT and OPR-constrained cycle offered by the SOFC, would be very hard to be achieved with conventional cycles at higher temperatures.

## 8 Conclusions and Outlook

A novel analysis of the aerothermal impacts and maximum benefit potential of GT-SOFC cycles has been proposed with the use of GT,

SOFC, and IPPS coupling variables, formulation of the integration constraints including the  $DoH_{el}$ ,  $DoH_{thermo}$ , BleedRatio, and  $H_2$ Ratio, and creation of universal coupling maps.

### Aerothermal effects

- The oxygen fraction at the combustor inlet after remixing the SOFC cathode products can drop from 23.3% to 16%. This oxygen reduction has a negligible thermodynamic impact in terms of  $C_p$ , but potential implications on the combustion performance and stability limits with oxygen-depleted air and steam require further investigation.
- The total WAR at the combustor outlet increases with an increasing  $DoH_{el}$ , although the WAR due to the combustion reduces. For the same  $DoH_{el}$ , the smaller-core gas turbine designs (higher TET) have higher WAR. However, in terms of  $NO_x$  emissions, it should be further investigated if the increase in SOFC-steam WAR and reduction in FAR is more dominant than the increase in the combustor inlet temperature in influencing the balance toward increasing or decreasing the  $NO_x$  emission index.
- The increase in the combustor outlet  $C_p$  due to the combined WAR due to SOFC steam and hydrogen combustion can be up to 6% compared to the hydrogen-combustion-only GT. This increase in WAR will be beneficial for the sizing of the turbines as it increases the specific enthalpy change but may negatively affect the gas turbine hot section life due to water vapor corrosion at high temperatures.
- Coupling regions allowing SOFC  $H_2O$  condensation due to the mixing with the cooler  $H_2$  from the fuel system were identified. A transition between the extremes, from dry steam to all liquid, can cause an efficiency drop up to 1–2% at high  $DoH_{el}$  because it is associated with higher WAR. The possibility of condensation increases and shifts toward higher temperature at higher FU, lower  $DoH_{el}$ , lower fuel system delivery temperature, and higher OPR, which causes higher  $H_2O$  partial pressure. However, condensation can be beneficial in further reducing  $NO_x$ . Development of two-phase  $H_2O$  models may be needed in future design studies falling in the regions of wet steam formation.
- Different combinations of electrical degree of hybridization and thermodynamic degree of hybridization can be achieved for different TET,  $U_{cell}$ , and  $AU$  combinations, highlighting the additional degrees of freedom in a GT-SOFC system. Although the overall efficiency change can be considered a unique function of  $DoH_{el}$ , the  $U_{cell}$  affects the tradeoff between SOFC electrical efficiency and GT thermodynamic hybridization and thermal efficiency. Depending on the  $NO_x$  emission targets and sizing choice of the SOFC for excess air, different splits in thermal power and electric power can be achieved for the SOFC although the overall efficiency will be nearly constant.
- The investigated TETs 1200–1800 K have optimal OPRs that are above 10. At 1300 K, the optimum OPR is around 22, and higher TET cycles have even higher optimum OPRs. Also, with increasing OPR, the HPC outlet temperature increases and the HEX can be sized for lower target effectiveness, which is beneficial. However, it is uncertain if the SOFC technology will evolve to operating pressures higher than 10 atm. SOFC pressure over 10 atm has a diminishing rate of return on SOFC performance improvement, and combined with the need for a heavier pressure vessel, a tradeoff analysis of overall efficiency vs overall mass (including the pressure vessel) must be performed at the aircraft level.

### Cycle potential

- The analysis revealed for the first time a nearly unique and universal relationship between  $DoH_{el}$  and absolute change in overall efficiency compared to  $DoH_{el}=0\%$ . The increase in efficiency can reach up to  $\Delta\eta=+22\%$  for  $DoH_{el}$  of 60%. This represents a significant and step change in the efficiency of

current and future propulsion systems and offers the potential for system efficiencies in the region of 45–65% (depending on the baseline cycle and SOFC).

- For a given  $\text{DoH}_{\text{el}}$  the change in efficiency is nearly independent of the gas turbine design parameters and SOFC AU, with only a very small impact coming from the Ucell and the GT specific power as they both affect the WAR due to the SOFC steam and the phase of the  $\text{H}_2\text{O}$ . However, the GT and SOFC parameters have a primary effect in determining the maximum achievable  $\text{DoH}_{\text{el}}$  based on their air flow matching. The maximum  $\text{DoH}_{\text{el}}$  is reduced when the TET increases, while higher  $\text{DoH}_{\text{el}}$  can be achieved when the Ucell and AU are increased. A maximum value of  $\text{DoH}_{\text{el}} \sim 78\%$  was identified for future, optimistic SOFC technology and low specific power cycles.
- At very high  $\text{DoH}_{\text{el}} > 45\%$  combined with very high  $dT > 250\text{K}$  through the SOFC system, the excess unutilized  $\text{H}_2$  and very high hydrogen temperature can become a limiting factor, but in most cases, this can be managed by adapting the fuel utilization factor of the SOFC or even reducing the fuel system delivery temperature.
- In the proposed universal GT-SOFC coupling design maps, the first map indicates the maximum efficiency change potential as a function of the  $\text{DoH}_{\text{el}}$  and the Ucell, while the second map is used to find the maximum  $\text{DoH}_{\text{el}}$  based on the GT and SOFC designs. The GT and SOFC designs are represented with their specific powers. Then, GT and SOFC design maps are proposed to map out what needs to be done to achieve different specific power and  $\text{DoH}_{\text{el}}$  targets on the coupling maps.
- Within the investigated AU and Ucell range, low specific power GT-SOFC cycles can have SFC reduction up to  $-55.7\%$  with optimized OPR and up to  $-53\%$  with constrained OPR=10, when comparing against  $\text{H}_2\text{GT}$  cycles with same TET and optimized OPR. High specific power GT-SOFC cycles can have SFC reduction up to  $-30\%$  with optimized OPR and up to  $-14.1\%$  with constrained OPR, when comparing against  $\text{H}_2\text{GT}$  cycles with same TET and optimized OPR. With constrained OPR, low specific power GT-SOFC cycles reach higher efficiencies than high specific power GT-SOFC cycles. OPR-constrained, low-specific power GT-SOFC cycles always outperform OPR-optimized low specific power GT-only cycles of the same TET. However, for  $\text{TET} > 1500\text{ K}$ , the OPR-constrained high-specific power GT-SOFC cycles have lower efficiency than the OPR-optimized GT-only cycles of the same TET, if the  $\text{AU} = 0.1$ . The GT-SOFC OPR-constrained high-specific power cycles can outperform OPR-optimized GT-only cycles of same TET, if the AU is increased toward 0.3. If future SOFC technology does not have a pressure constraint, all cycles can get considerable benefits from SOFC integration, even with conservative  $\text{Ucell} = 0.6\text{V}$  and  $\text{AU} = 0.1$ .
- The thermodynamic optimum for each cycle was found at  $\text{AU} = 0.3$  and  $\text{Ucell} = 0.9\text{ V}$ ; however, an aircraft-level energy analysis is required to capture the tradeoff between the efficiency gain and system mass. An increase in AU from 0.1 to 0.3 was found to be more impactful in increasing the  $\text{DoH}_{\text{el}}$  and the potential combined efficiency gain, than increasing the Ucell from 0.6 V to 0.9 V. Considering that for a given cell technology, a higher Ucell requires more cells and results in a higher mass penalty, it is expected that a compromise should be made at a lower Ucell than 0.9V to contain the mass penalty, as long as a sufficient degradation margin is provided to meet the target time on wing. Future SOFC technology development should prioritize increasing the AU to unlock higher  $\text{DoH}_{\text{el}}$ , while compromising on the Ucell due to the mass tradeoff. Higher AU with low/medium Ucell results in higher  $dT$  through the cell  $> 150\text{ K}$ . This overheating could potentially be dealt with by increasing the cell length, thus reducing the thermal gradient, and using improved materials.

- Finally, the paper identifies four design regions for turboprops and open rotors covering a wide range of applications from commuter aircraft to future ultra-efficient, single-aisle aircraft. Larger turboshaft applications with high specific power cycles, although their relative margin of improvement is lower than for low specific power cycles, with advanced SOFC integration ( $\text{AU} = 0.3$ ) they still have the potential to achieve efficiency gains in the range of  $+7\text{--}20\%$  (depending on the SOFC pressure constraint) which would be otherwise hard to be realized with conventional cycles as they are already close to the material limitations.

## Acknowledgment

The authors would like to thank the FlyECO consortium for the overall collaboration and support in the project and architecture definition.

Views and opinions expressed are, however, those of the author(s) only and do not necessarily reflect those of the European Union, UKRI or CINEA. Neither the European Union nor the granting authorities can be held responsible for them.

## Conflict of Interest

There are no conflicts of interest.

## Funding Data

- European Union's Horizon Europe Research and Innovation Program (Grant Agreement No. 101138488; Funder ID: 10.13039/501100000780).
- UK Research and Innovation (UKRI) Funding Guarantee (Project Reference 10106893; Funder ID: 10.13039/100014013).

## Nomenclature

|                    |                                                                                 |
|--------------------|---------------------------------------------------------------------------------|
| $A_{\text{cell}}$  | = cell area                                                                     |
| AU                 | = air utilization factor                                                        |
| $C$                | = heat capacity rate of HEX hot or cold side flow ( $\dot{m} \cdot \text{Cp}$ ) |
| $C_p$              | = specific heat capacity under constant pressure                                |
| DoH                | = degree of hybridization                                                       |
| $F$                | = Faraday constant                                                              |
| FAR                | = fuel-to-air ratio                                                             |
| FU                 | = fuel utilization factor                                                       |
| GT                 | = gas turbine                                                                   |
| $\dot{H}$          | = enthalpy flow                                                                 |
| IPPS               | = integrated power and propulsion system                                        |
| $j$                | = current density                                                               |
| LHV                | = lower heating value                                                           |
| $\dot{m}$          | = mass flow                                                                     |
| $m_{\text{c,rel}}$ | = cooling fraction (relative to the high-pressure compressor air flow)          |
| MW                 | = molecular weight                                                              |
| $N_{\text{cells}}$ | = number of cells                                                               |
| NPP                | = nonpropulsive power                                                           |
| OPR                | = overall pressure ratio                                                        |
| $P$                | = pressure (total)                                                              |
| PD                 | = power density (output power/mass)                                             |
| PW                 | = power                                                                         |
| $R_{\text{NPP}}$   | = relative power penalty for nonpropulsive (auxiliary) power                    |
| SFC                | = specific fuel consumption                                                     |
| SOFC               | = solid oxide fuel cell                                                         |
| SP                 | = specific power (output power/mass flow)                                       |
| $T$                | = temperature                                                                   |
| TET                | = turbine entry temperature                                                     |
| Ucell              | = cell voltage                                                                  |
| WAR                | = water/steam to air ratio                                                      |
| $y_{\text{O}_2}$   | = mass fraction of $\text{O}_2$ in air                                          |
| $z$                | = valence electrons                                                             |

## Greek Symbols

$\epsilon$  = heat exchanger effectiveness  
 $\eta$  = efficiency

## Superscripts and Subscripts

ano = anode  
 aux = auxiliary  
 c = cold side or cooling  
 cat = cathode  
 el = electrical  
 FT = free turbine  
 h = hot side  
 HPT = high-pressure turbine  
 poly = polytropic  
 prop = propeller

## Appendix A: Gas Turbine Interfaces With System

**A.1 Air Supply to the Solid Oxide Fuel Cell.** The air mass flow to the SOFC, which is provided by the GT main gas path, needs to account for the propulsive and nonpropulsive power

$$\dot{m}_{\text{air,SOFC,in}} = \frac{PW_{\text{SOFC,el,prop}} + PW_{\text{aux}}}{U_{\text{cell}} \cdot z_{O_2} \cdot F \cdot AU \cdot y_{O_2}} \cdot MW_{\text{air}} \quad (\text{A1})$$

This air enters the SOFC system—specifically, the cathode HEX cold side inlet—at the HPC outlet temperature and pressure as calculated by the gas turbine performance simulation.

**A.2 Cathode Air Remixing.** The returning cathode air that is remixed back to GT main path is reduced according to the utilized oxygen (Eq. (A2))

$$\dot{m}_{\text{air,SOFC,out}} = \dot{m}_{\text{air,SOFC,in}} \cdot (1 - y_{O_2} \cdot AU) \quad (\text{A2})$$

The returning cathode gas is at the cathode HEX hot side outlet temperature (Eq. (A3)). Therefore, there is a temperature drop from the stack outlet temperature in order to preheat the incoming air supply from the HPC to the SOFC stack

$$T_{\text{cat,HEX,hot,out}} = T_{\text{cat,HEX,hot,in}} - \epsilon \cdot \frac{\dot{m}_c \cdot C_{p_c} \cdot (T_{\text{cat,HEX,hot,in}} - T_{\text{cat,HEX,cold,in}})}{\dot{m}_h \cdot C_{p_h}} \quad (\text{A3})$$

By replacing the cold side and hot side mass flows with Eqs. (A1) and (A2), respectively, the cold side inlet temperature with the HPC outlet temperature and the hot side outlet temperature with the SOFC stack outlet, Eq. (A3) transforms to

$$T_{\text{cat,HEX,hot,out}} = T_{\text{SOFC,stack,out}} - \epsilon \cdot \frac{C_{p_c} \cdot (T_{\text{SOFC,stack,out}} - T_{\text{HPC,out}})}{(1 - y_{O_2} \cdot AU) \cdot C_{p_h}} \quad (\text{A4})$$

**A.3 Anode Product Remixing.** The inlet hydrogen flow and produced steam of the SOFC is calculated using Faraday's law in the following equations:

$$\dot{m}_{\text{H}_2,\text{SOFC,in}} = \frac{PW_{\text{SOFC,el,prop}} + PW_{\text{aux}}}{U_{\text{cell}} \cdot z_{H_2} \cdot F \cdot FU \cdot y_{H_2}} \cdot MW_{H_2} \quad (\text{A5})$$

$$\dot{m}_{\text{H}_2\text{O,SOFC,out}} = \frac{PW_{\text{SOFC,el,prop}} + PW_{\text{aux}}}{U_{\text{cell}} \cdot z_{H_2} \cdot F \cdot y_{H_2}} \cdot MW_{H_2\text{O}} \quad (\text{A6})$$

The consumed and the unutilized hydrogen mass flow can be determined in relation to the total hydrogen inlet mass flow to the SOFC

$$\dot{m}_{\text{H}_2,\text{SOFC,consumed}} = \dot{m}_{\text{H}_2,\text{SOFC,in}} \cdot FU \quad (\text{A7})$$

$$\dot{m}_{\text{H}_2,\text{SOFC,out}} = \dot{m}_{\text{H}_2,\text{SOFC,in}} \cdot (1 - FU) \quad (\text{A8})$$

The temperature of the mixed  $\text{H}_2/\text{H}_2\text{O}$  coming from the SOFC anode outlet and the additional  $\text{H}_2$  coming from the fuel system to provide the total needed for combustion is calculated with the enthalpy balance of the adiabatic, isentropic mixing by resolving Eq. (A9) for  $T_{\text{H}_2/\text{H}_2\text{O,mixed}}$  when all species remain in the gas phase

$$\begin{aligned} \dot{H}_1 = \dot{H}_2 &\rightarrow \Delta \dot{H} \\ &= 0 \rightarrow \dot{m}_{\text{H}_2\text{O,SOFC,out}} \cdot C_p \cdot (T_{\text{ano,HEX,h,out}} - T_{\text{H}_2/\text{H}_2\text{O,mixed}}) \\ &\quad + \dot{m}_{\text{H}_2,\text{SOFC,out}} \cdot C_p \cdot (T_{\text{ano,HEX,h,out}} - T_{\text{H}_2/\text{H}_2\text{O,mixed}}) \\ &\quad + \dot{m}_{\text{H}_2,\text{fuelsystem}} \cdot C_p \cdot (T_{\text{fuel,system}} - T_{\text{H}_2/\text{H}_2\text{O,mixed}}) = 0 \end{aligned} \quad (\text{A9})$$

where the  $C_p$  of each species is a function of the mean temperature for each heat exchange process according to Fig. 4.

However, during the  $\text{H}_2/\text{H}_2\text{O}$  mixing, the  $\text{H}_2\text{O}$  partial pressure and low mixing temperature due to the fuel system temperature being at 300 K, can bring the  $\text{H}_2\text{O}$  below its saturation point and phase change may take place. In that case, in Eq. (A9), the  $\text{H}_2\text{O}$  enthalpy change must consider two parts; the  $\text{H}_2\text{O}$  vapor and the condensed liquid  $\text{H}_2\text{O}$  and Eq. (A9) becomes (A10)

$$\begin{aligned} \dot{H}_1 = \dot{H}_2 &\rightarrow \Delta \dot{H} = 0 \rightarrow \dot{m}_{\text{H}_2\text{O,SOFC,out}} \cdot h(T_{\text{ano,HEX,h,out}}) \\ &\quad + \dot{m}_{\text{H}_2,\text{SOFC,out}} \cdot h(T_{\text{ano,HEX,h,out}}) + \dot{m}_{\text{H}_2,\text{fuelsystem}} \cdot h(T_{\text{fuel,system}}) \\ &= \dot{m}_{\text{H}_2,\text{GT,in}} \cdot h(T_{\text{H}_2/\text{H}_2\text{O,mixed}}) \\ &\quad + \dot{m}_{\text{H}_2\text{O,SOFC,out}} \cdot \left( x \cdot h_g(T_{\text{H}_2/\text{H}_2\text{O,mixed}}) \right. \\ &\quad \left. + (1 - x) \cdot h_l(T_{\text{H}_2/\text{H}_2\text{O,mixed}}) \right) \end{aligned} \quad (\text{A10})$$

where  $x$  is the  $\text{H}_2\text{O}$  vapor fraction to the total  $\text{H}_2\text{O}$ . When  $x = 0$  the  $\text{H}_2\text{O}$  is liquid, and when  $x = 1$ , the  $\text{H}_2\text{O}$  is saturated, dry steam. When  $0 < x < 1$ , the steam is wet and  $T_{\text{H}_2/\text{H}_2\text{O,mixed}} = T_{\text{sat,H}_2\text{O}}$ .

The saturation point of  $\text{H}_2\text{O}$  is calculated using the Antoine equation from Ref. [40].

## Appendix B: Figures of Merit Calculation

The thermal efficiency of a turboshaft is calculated with the following equation:

$$\eta_{\text{GT}} = \frac{PW_{\text{GT}}}{\dot{m}_{\text{H}_2,\text{GT,total}} \cdot \text{LHV}} \quad (\text{B1})$$

The SOFC electrical efficiency is based on the consumed hydrogen energy without considering the utilization losses (Eq. (B2))

$$\eta_{\text{SOFC,el}} = \frac{PW_{\text{SOFC}}}{\dot{m}_{\text{H}_2,\text{SOFC,in}} \cdot FU \cdot \text{LHV}} \quad (\text{B2})$$

The overall efficiency is calculated as the ratio of total propulsive power produced by the system to the total fuel power consumed by the system. However, attention is needed not to double-account for the SOFC unutilized hydrogen which is provided initially to the SOFC and then to the GT

$$\eta_{\text{overall}} = \frac{PW_{\text{GT}} + PW_{\text{el,prop}}}{(\dot{m}_{\text{H}_2,\text{SOFC,in}} \cdot FU + \dot{m}_{\text{H}_2,\text{GT}}) \cdot \text{LHV}} \quad (\text{B3})$$

The (power) specific fuel consumption of the GT alone and of the hybrid system is defined with the following equations, respectively,

$$GT\ SFC = \frac{\dot{m}_{H_2,GT,total}}{PW_{GT}} \quad (B4)$$

$$IPPS\ SFC = \frac{\dot{m}_{H_2,SOFC,consumed} + \dot{m}_{H_2,GT}}{PW_{IPPS}} \quad (B5)$$

### Appendix C: Gas Properties With NIST Polynomials

Using the National Institute of Standards and Technology (NIST) polynomials for thermochemistry properties [41], the  $C_p$  over temperature is calculated for  $O_2$ ,  $N_2$ , and  $H_2O$  in the gas and liquid phases (Fig. 29).

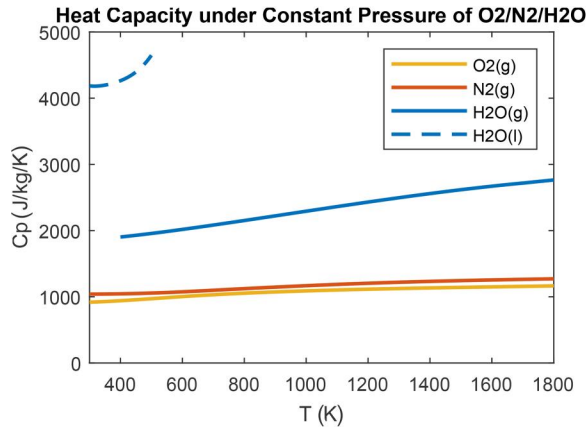


Fig. 29 Heat capacity under constant pressure of  $O_2$ ,  $N_2$ , and  $H_2O$  at 300–1800 K

### Appendix D: Heat Exchanger Target Effectiveness

**D.1 Cathode Heat Exchanger.** At the design point of the HEX, the required heat exchanger effectiveness is calculated to meet the target SOFC inlet temperature, which is set at 700 °C.

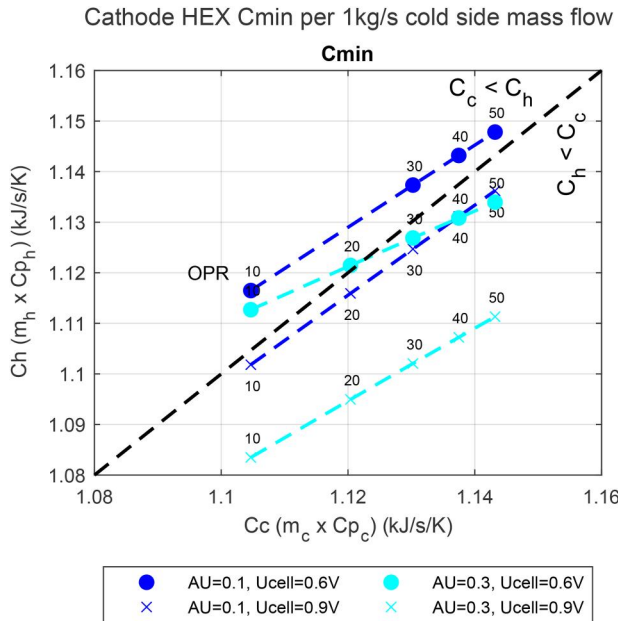


Fig. 30 Cathode HEX cold side and hot side heat capacity rate to identify HEX Cmin under variable AU, Ucell, and OPR

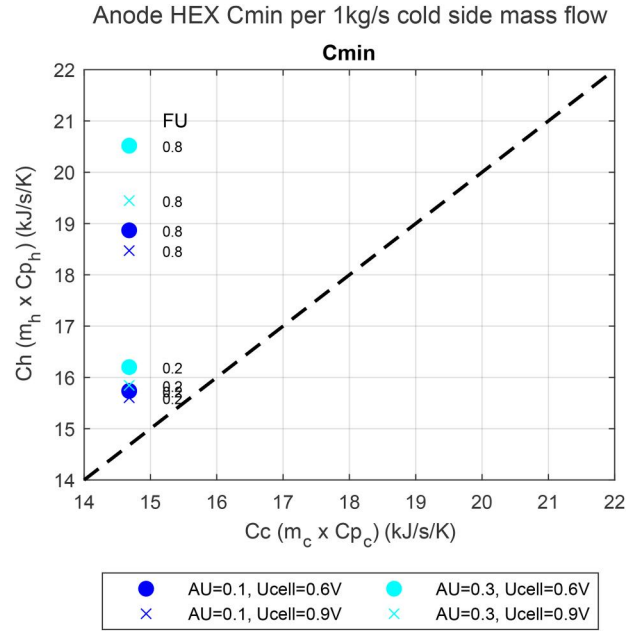


Fig. 31 Anode HEX cold side and hot side heat capacity rate to identify HEX Cmin under variable AU, Ucell and FU

For low  $AU = 0.1$  and low  $U_{cell} = 0.6V$ , the Cmin is at the cold side of the cathode HEX, as the increase in  $C_p$  due to the hotter stack outlet temperature is sufficient to overcompensate for the small drop in the mass flow of the hot side due to the oxygen consumption. For high  $AU = 0.3$ , which further reduces the hot side mass flow and high  $OPR > 20$ , which increases the cold side inlet temperature, the Cmin can shift to the hot side of the cathode HEX. When  $U_{cell} = 0.9V$ , the hot side always has the Cmin because of the cooler stack outlet temperature causing lower hot side  $C_p$  which cannot overcompensate for the lower hot side mass flow (Figs. 30 and 31). Equations (D1), (D2) and (D5) have been written assuming the Cmin is at the cold side, but the denominator can be updated with the hot side, when appropriate, based on Fig. 30.

$$\epsilon = \frac{\Delta \dot{H}_{actual}}{\Delta \dot{H}_{ideal}} = \frac{\dot{m}_{SOFC,air,in} \cdot C_{p_c} \cdot (T_{c,out} - T_{c,in})}{\dot{m}_{SOFC,air,in} \cdot C_{p_c} \cdot (T_{h,in} - T_{c,in})} \quad (D1)$$

$$\epsilon = \frac{\Delta \dot{H}_{actual}}{\Delta \dot{H}_{ideal}} = \frac{\dot{m}_{SOFC,air,out} \cdot C_{p_h} \cdot (T_{h,in} - T_{h,out})}{\dot{m}_{SOFC,air,in} \cdot C_{p_c} \cdot (T_{h,in} - T_{c,in})} \quad (D2)$$

$$\dot{m}_{SOFC,air,in} \cdot C_{p_c} \cdot (T_{c,out} - T_{c,in}) = \dot{m}_{SOFC,air,out} \cdot C_{p_h} \cdot (T_{h,in} - T_{h,out}) \quad (D3)$$

where the outlet air of the SOFC can be replaced with

$$\dot{m}_{SOFC,air,out} = \dot{m}_{SOFC,air,in} \cdot (1 - y_{O_2} \cdot AU) \quad (D4)$$

The heat balance can be decoupled from the actual mass and only use a relative relationship between the hot side and cold side mass based on the air utilization factor

$$\epsilon = (1 - y_{O_2} \cdot AU) \cdot \frac{C_{p_h} \cdot (T_{h,in} - T_{h,out})}{C_{p_c} \cdot (T_{h,in} - T_{c,in})} \quad (D5)$$

where

- $T_{c,in} = T_{HPC,out}$
- $T_{c,out} = T_{SOFC,stack,in}$
- $T_{h,in} = T_{SOFC,stack,out}$
- $T_{h,out} = T_{cat,HEX,out}$

The  $C_p$  is calculated at each side ( $C_{p_c}$  and  $C_{p_h}$ ) of the HEX based on the gas mixture and average temperature between the inlet and outlet of each side.

**D.2 Anode Heat Exchanger.** Similar to the cathode HEX, the target HEX effectiveness for the anode is calculated. Within the investigated range, the Cmin for the anode HEX was always at the cold side because the conversion of  $H_2$  to  $H_2O$  increases the converted mass flow by nine and the cold side inlet always remains at the fuel system delivery temperature (assumed at 300 K) which is much lower than the hot side inlet

$$\epsilon = \frac{C_{p_c} \cdot (T_{c,out} - T_{c,in})}{C_{p_c} \cdot (T_{h,in} - T_{c,in})} \quad (D6)$$

$$\epsilon = \frac{(1 - FU + FU \cdot MW_{H_2O}/MW_{H_2}) \cdot C_{p_h} \cdot (T_{h,in} - T_{h,out})}{C_{p_c} \cdot (T_{h,in} - T_{c,in})} \quad (D7)$$

where

- $T_{c,in} = T_{\text{fuelsystem}}$
- $T_{c,out} = T_{\text{SOFC,stack,in}}$
- $T_{h,in} = T_{\text{SOFC,stack,out}}$
- $T_{h,out} = T_{\text{ano,HEX,out}}$

Figure 31 assumes the  $H_2O$  as superheated steam for the Cp calculation. Liquid water or wet steam would further increase the hot side Cp, therefore, the Cmin would still be at the cold side.

## Appendix E: Supplementary Graphs

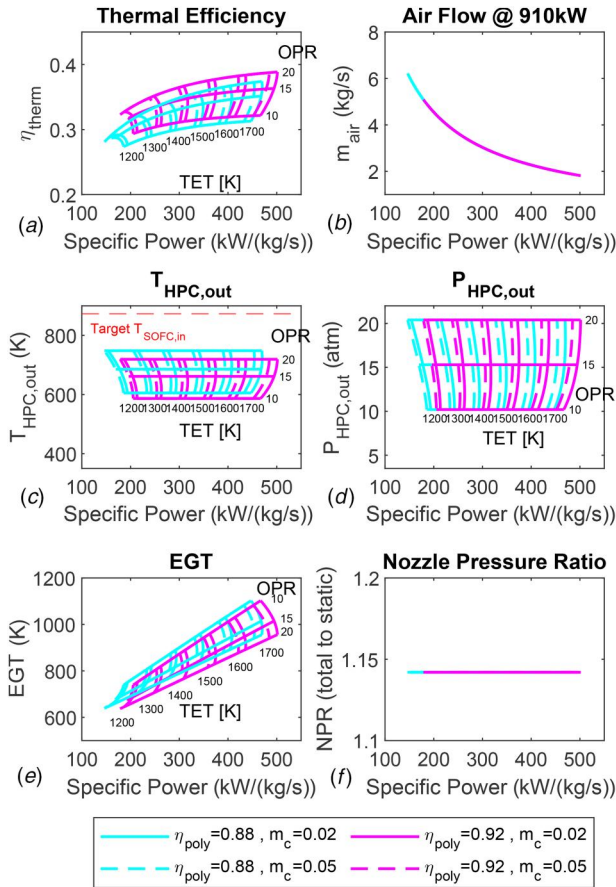


**Fig. 32** Baseline  $H_2GT$  design and technology sensitivity at take-off for a commuter class aircraft

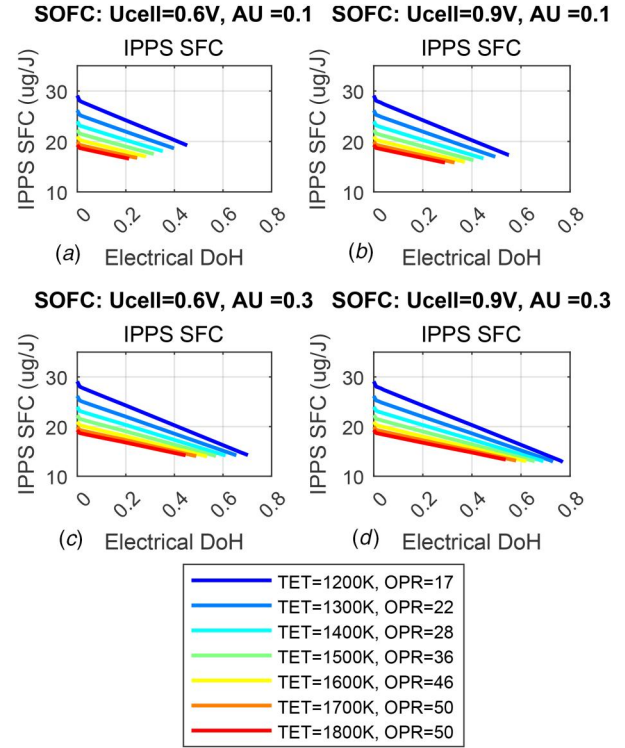


**Fig. 33** Combined SFC for different cycles when the OPR is optimum for the TET for (a)  $U_{\text{cell}} = 0.6V$  and  $AU = 0.1$ , (b)  $U_{\text{cell}} = 0.9V$  and  $AU = 0.1$ , (c)  $U_{\text{cell}} = 0.6V$  and  $AU = 0.3$ , and (d)  $U_{\text{cell}} = 0.9V$  and  $AU = 0.3$

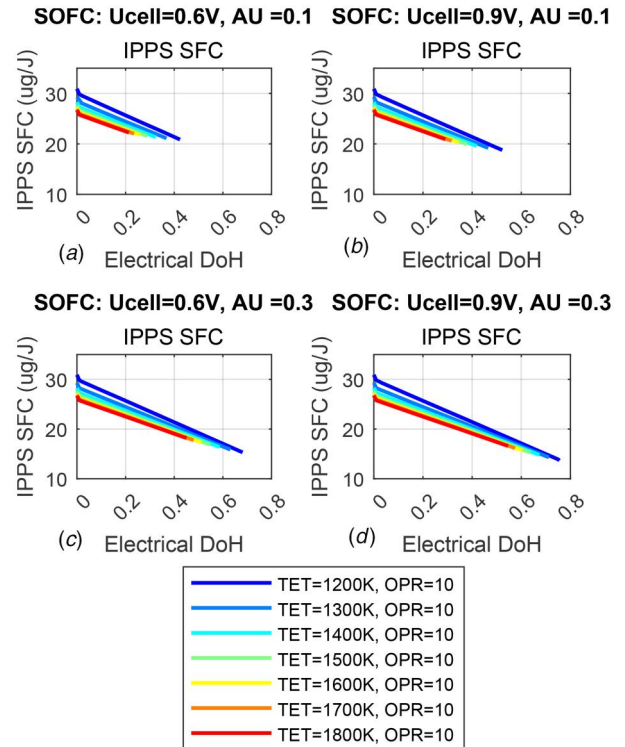


**Fig. 34** Combined SFC for different cycles when the OPR is constrained at 10 for (a)  $U_{\text{cell}} = 0.6V$  and  $AU = 0.1$ , (b)  $U_{\text{cell}} = 0.9V$  and  $AU = 0.1$ , (c)  $U_{\text{cell}} = 0.6V$  and  $AU = 0.3$ , and (d)  $U_{\text{cell}} = 0.9V$  and  $AU = 0.3$

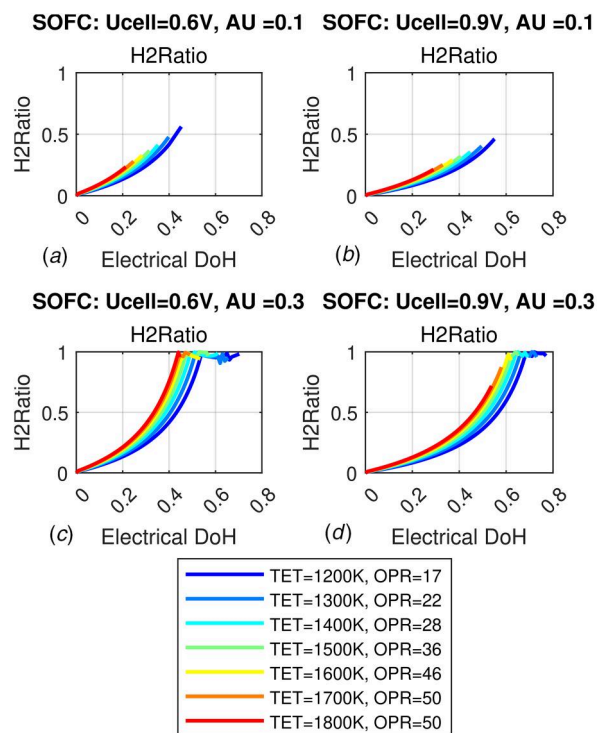


Fig. 35  $H_2$  Ratio against  $DoH_{el}$  when the OPR is optimum for the TET for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

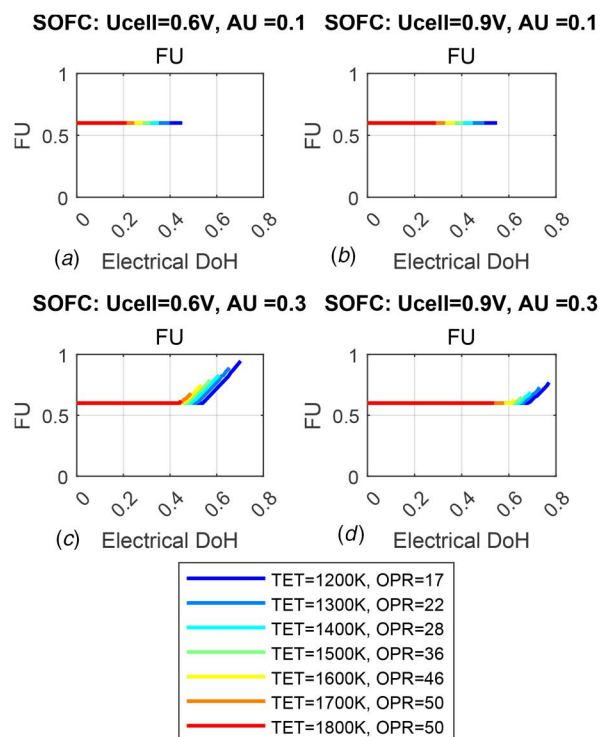


Fig. 37 FU against  $DoH_{el}$  when the OPR is optimum for the TET for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

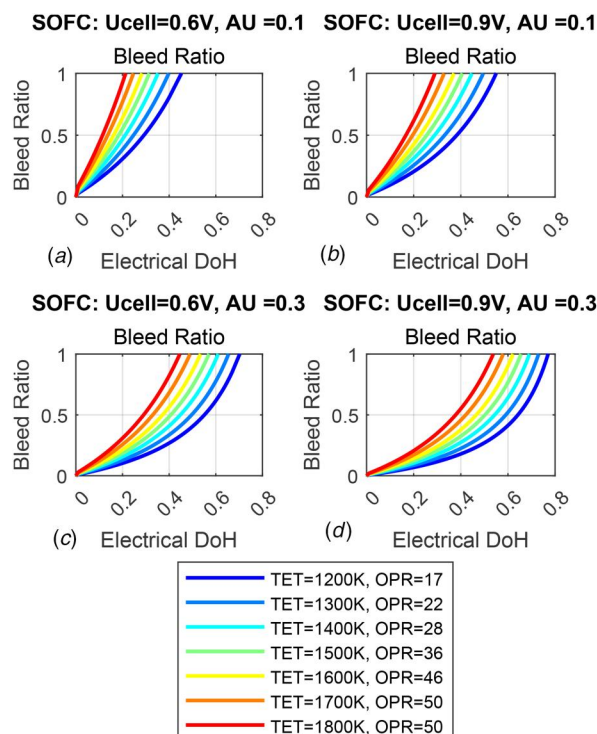


Fig. 36 Bleed ratio against  $DoH_{el}$  when the OPR is optimum for the TET for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

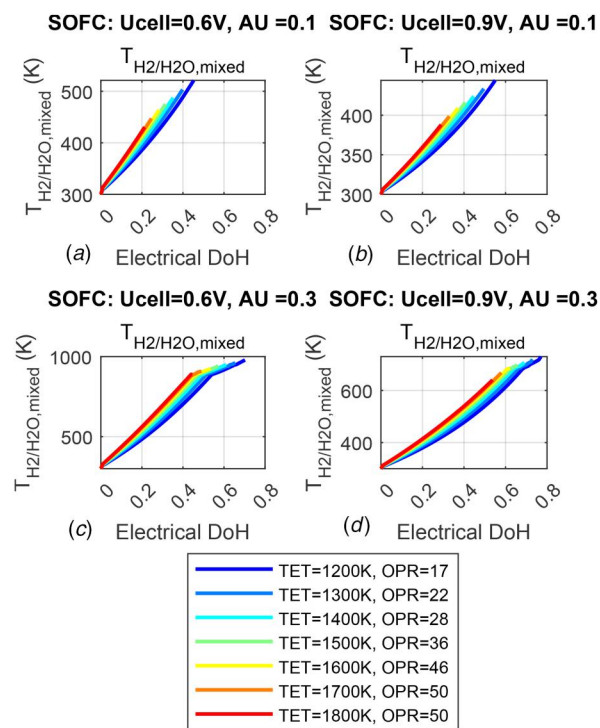


Fig. 38  $T_{H_2/H_2O,mixed}$  against  $DoH_{el}$  when the OPR is optimum for the TET for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

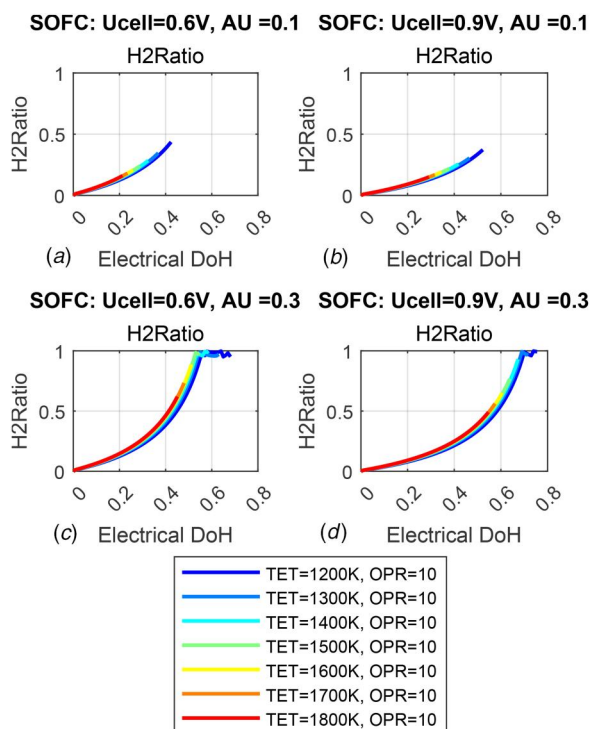


Fig. 39  $H_2$ Ratio against  $DoH_{el}$  when the OPR is constrained at 10 for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

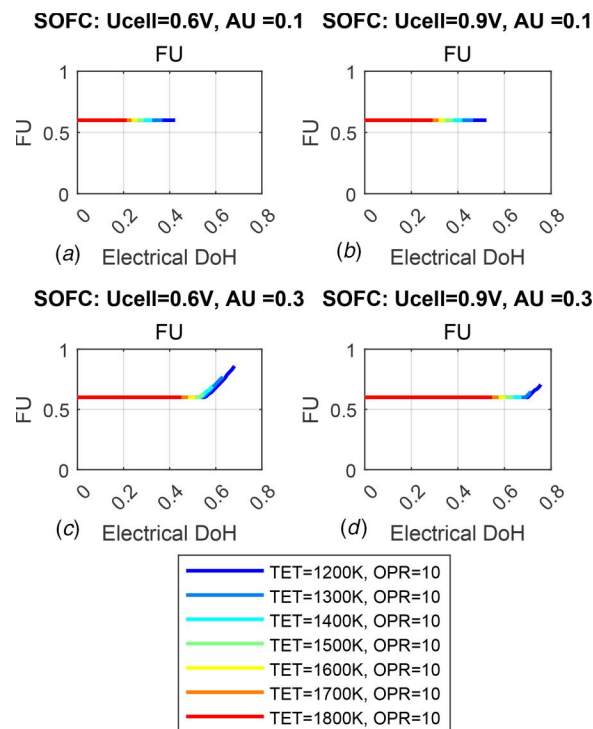


Fig. 41 FU against  $DoH_{el}$  when the OPR is constrained at 10 for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

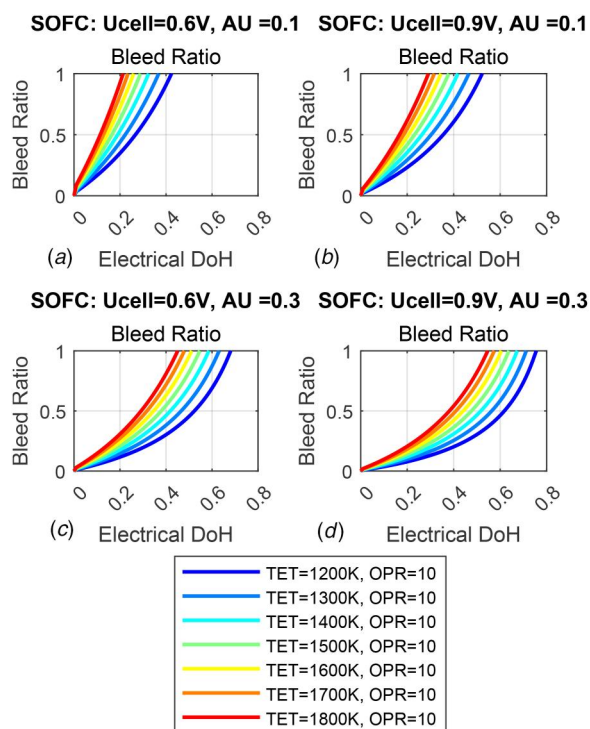


Fig. 40 Bleed ratio against  $DoH_{el}$  when the OPR is constrained at 10 for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

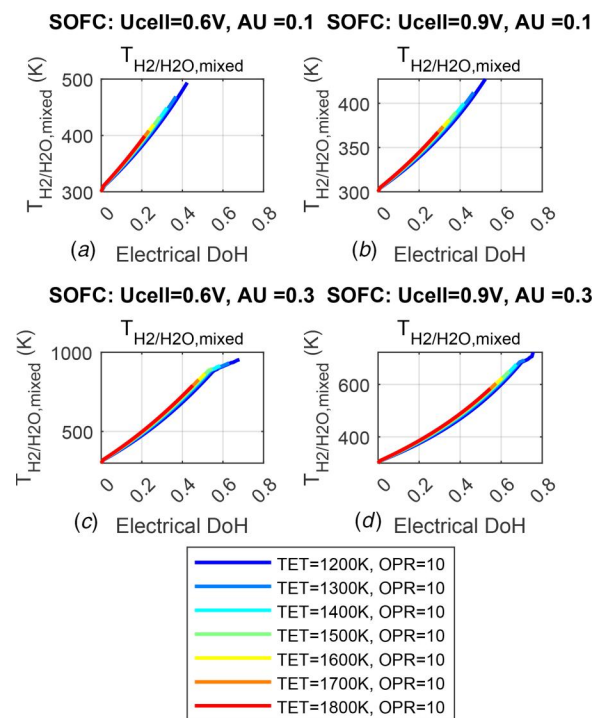


Fig. 42  $T_{H_2/H_2O,mixed}$  against  $DoH_{el}$  when the OPR is constrained at 10 for (a) Ucell = 0.6V and AU = 0.1, (b) Ucell = 0.9V and AU = 0.1, (c) Ucell = 0.6V and AU = 0.3, and (d) Ucell = 0.9V and AU = 0.3

## References

- [1] Zhang, X., Chan, S., Li, G., Ho, H., Li, J., and Feng, Z., 2010, "A Review of Integration Strategies for Solid Oxide Fuel Cells," *J. Power Sources*, **195**(3), pp. 685–702.
- [2] Fernandes, M. D., de P. Andrade, S. T., Bistrizki, V. N., Fonseca, R. M., Zacarias, L. G., Gonçalves, H. N., de Castro, A. F., Domingues, R. Z., and Matencio, T., 2018, "SOFC-APU Systems for Aircraft: A Review," *Int. J. Hydrogen Energy*, **43**(33), p. 16311.
- [3] Mashamba, T. M., Wen, J., Spataru, C., Weng, Y., and Lv, X., 2024, "Characteristic Investigation of a Novel Aircraft SOFC/GT Hybrid System Under Varying Operational Parameters," *Appl. Sci.*, **14**(8), p. 3504.
- [4] Collins, J. M., and McLarty, D., 2020, "All-Electric Commercial Aviation With Solid Oxide Fuel Cell-Gas Turbine-Battery Hybrids," *Appl. Energy*, **265**, p. 114787.
- [5] Köhler, P., Tillner, J., and Kabelac, S., 2024, "Thermodynamic Assessment of Hydrogen-Fueled Solid Oxide Fuel Cell - Gas Turbine (SOFC-GT) Systems for Low-Emission Aircraft Propulsion," Deutsche Gesellschaft für Luft- und Raumfahrt - Lilienthal-Oberth e.V., Bonn, Germany.
- [6] Köhler, P., Hollmann, J., Taissir, A., Heddrich, M. P., and Kabelac, S., 2026, "Future Highly Efficient Engines With Solid Oxide Fuel Cell-Gas Turbine Coupling: System Modeling Study and Comparison of Directly and Indirectly Coupled SOFC-GT Systems," *Aerospace*, **13**(3), p. 263.
- [7] Geuther, S., and Capristan, F., 2020, "Feasibility of a Solid Oxide Fuel Cell System Applied to Hybrid-Electric Regional Aircraft," *AIAA* Paper No. 2020-1500.
- [8] Waters, D. F., Pratt, L. M., and Cadou, C. P., 2021, "Gas Turbine/Solid Oxide Fuel Cell Hybrids for Aircraft Propulsion and Power," *J. Propul. Power*, **37**(3), pp. 349–361.
- [9] Freeh, J. E., Steffen, C. J., and Larosiliere, L. M., 2005, "Off-Design Performance Analysis of a Solid-Oxide Fuel Cell/Gas Turbine Hybrid for Auxiliary Aerospace Power," *ASME* Paper No. FUELCELL2005-74099.
- [10] Wagner, L., and Tingas, E.-A., 2025, "Computational Analysis of an Ammonia-Fuelled Hybrid Solid Oxide Fuel Cell-Gas Turbine Propulsion System for Commercial Aviation," *Energy Convers. Manage.*, **336**, p. 119861.
- [11] Mantelli, L., Dubey, A., Buzzola, D., Ferrari, M. L., Pontika, E., Kazula, S., Ewald, D., Weber, A., and De Graaf, S., 2025, "Methodology for Exploring SOFC System Layouts in a Highly Integrated Hybrid Propulsion System," *ASME* Paper No. GT2025-151884.
- [12] San, D., Pastor, B., Pontika, E., Ewald, D., Weber, A., Kazula, S., and Ferrari, M. L., 2025, "Hydrogen-Powered Integrated Power and Propulsion System (IPPS) Architecture for Sustainable Aviation: Leveraging Synergies Between Gas Turbine and Solid Oxide Fuel Cells," *Towards Sustainable Aviation Summit 2025*, Association Aeronautique et Astronautique de France, Toulouse, France.
- [13] McMillan, W., 1974, "Development of a Modular Type Computer Program for the Calculation of Gas Turbine Off-Design Performance," Ph.D. thesis, Cranfield Institute of Technology, Cranfield, Bedford, UK, accessed Nov. 7, 2025, <http://dspace.lib.cranfield.ac.uk/handle/1826/7401>
- [14] Janikovic, J., 2010, "Gas Turbine Transient Performance Modeling for Engine Flight Path Cycle Analysis," Ph.D. thesis, Cranfield University, Cranfield, Bedford, UK, accessed Nov. 20, 2025, [https://dspace.lib.cranfield.ac.uk/bitstream/handle/1826/7894/Jan\\_Janikovic\\_Thesis\\_2010.pdf](https://dspace.lib.cranfield.ac.uk/bitstream/handle/1826/7894/Jan_Janikovic_Thesis_2010.pdf)
- [15] Nikolaidis, T., Pellegrini, A., Saravanamuttoo, H., Aslanidou, I., Kalfas, A., and Pilidis, P., 2020, "Off-Design Performance Comparison Between Single and Two-Shaft Engines: Part 1 – Fixed Geometry," *ASME* Paper No. GT2020-14500.
- [16] Walsh, P. P., and Fletcher, P., 2004, *Gas Turbine Performance*, 2nd ed., Blackwell Science Ltd., Oxford, UK.
- [17] Kurzke, J., and Halliwell, I., 2018, *Propulsion and Power*, Springer International Publishing, Cham, Switzerland, p. 766.
- [18] Henke, M., Willich, C., Westner, C., Leucht, F., Leibinger, R., Kallo, J., and Friedrich, K. A., 2012, "Effect of Pressure Variation on Power Density and Efficiency of Solid Oxide Fuel Cells," *Electrochim. Acta*, **66**, pp. 158–163.
- [19] Grosselindemann, C., Dorn, M., Bauer, F. M., Seim, M., Ewald, D., Esau, D., Georg, M., Rössler, R., Pundt, A., and Weber, A., 2024, "Pressurized Single Cell Testing of Solid Oxide Cells," *J. Power Sources*, **614**, p. 234963.
- [20] Sun, X., Sethi, V., Singh, G., Giannouloudis, A., Rolt, A., Abbott, D., Gauthier, P., et al., 2023, "ENABLEH2: D3.3 Final Report for the Single Sector H<sub>2</sub> Micromix Combustor Segment Performance and NO<sub>x</sub> Emissions Assessment," Cranfield University, Cranfield, Bedford, UK, accessed Apr. 10, 2026, <https://ec.europa.eu/research/participants/documents/downloadPublic?documentIds=080166e5f87040de&appId=PPGMS>
- [21] Mourouzidis, C., Singh, G., Sun, X., Huete, J., Nalianda, D., Nikolaidis, T., Sethi, V., Rolt, A., Goodger, E., and Pilidis, P., 2024, "Abating CO<sub>2</sub> and Non-CO<sub>2</sub> Emissions With Hydrogen Propulsion," *Aeronaut. J.*, **128**(1325), pp. 1576–1593.
- [22] Góke, S., and Paschereit, C. O., 2013, "Influence of Steam Dilution on Nitrogen Oxide Formation in Premixed Methane/Hydrogen Flames," *J. Propul. Power*, **29**(1), pp. 249–260.
- [23] Pouzol, R., Schmitz, O., and Klingels, H., 2021, "Evaluation of the Climate Impact Reduction Potential of the Water-Enhanced Turbofan (WET) Concept," *Aerospace*, **8**(3), p. 59.
- [24] Nehter, P., Geisler, H., Ahilan, V., Friedl, S., Rohr, O., Walter, A., Metzner, C., and Zimmermann, K., 2023, "Solid Oxide Fuel Cells for Aviation," *Electrochem. Soc. Trans.*, **111**(6), pp. 143–154.
- [25] Qasem, N. A., and Abdulrahman, G. A., 2024, "A Recent Comprehensive Review of Fuel Cells: History, Types, and Applications," *Int. J. Energy Res.*, **2024**(1), p. 7271748.
- [26] Udomsilp, D., Lenser, C., Guillon, O., and Menzler, N. H., 2021, "Performance Benchmark of Planar Solid Oxide Cells Based on Material Development and Designs," *Energy Technology*, **9**(4), p. 2001062.
- [27] Aguiar, P., Adjiman, C., and Brandon, N., 2005, "Anode-Supported Intermediate-Temperature Direct Internal Reforming Solid Oxide Fuel Cell: II. Model-Based Dynamic Performance and Control," *J. Power Sources*, **147**(1–2), pp. 136–147.
- [28] Kendall, K., 2010, "Progress in Microtubular Solid Oxide Fuel Cells," *Int. J. Appl. Ceram. Technol.*, **7**(1), pp. 1–9.
- [29] Lawlor, V., Griesser, S., Buchinger, G., Olabi, A., Cordiner, S., and Meissner, D., 2009, "Review of the Micro-Tubular Solid Oxide Fuel Cell: Part I. Stack Design Issues and Research Activities," *J. Power Sources*, **193**(2), pp. 387–399.
- [30] Timurkutluk, B., Timurkutluk, C., Mat, M. D., and Kaplan, Y., 2016, "A Review on Cell/Stack Designs for High Performance Solid Oxide Fuel Cells," *Renewable Sustainable Energy Rev.*, **56**, pp. 1101–1121.
- [31] Henke, M., Kallo, J., Friedrich, K. A., and Bessler, W. G., 2011, "Influence of Pressurisation on SOFC Performance and Durability: A Theoretical Study," *Fuel Cells*, **11**(4), pp. 581–591.
- [32] Whyatt, G. A., and Chick, L. A., 2012, "Electrical Generation for More-Electric Aircraft using Solid Oxide Fuel Cells," Pacific Northwest National Laboratory (PNNL), Richland, WA.
- [33] Corralejo, R., and Harley, P., 2017, "Smith Diagram for Low Reynolds Number Axial Compressor Rotors," *12th European Conference on Turbomachinery Fluid Dynamics & Thermodynamics*, Stockholm, Sweden, Apr. 3–7, pp. 1–15.
- [34] Pampreen, R. C., 1973, "Small Turbomachinery Compressor and Fan Aerodynamics," *ASME J. Eng. Power*, **95**(3), pp. 251–256.
- [35] Diehl, M., and Schiffmann, J., 2019, "Impact of large tip clearance ratios on the performance of a centrifugal compressor," *13th European Conference on Turbomachinery Fluid dynamics & Thermodynamics*, Lausanne, Switzerland, Apr. 8–12, Paper No. ETC2019-304.
- [36] Chapman, J. W., 2019, "Multipoint Design and Optimization of a Turbohaft Engine for a Tiltwing Turboelectric Vertical Takeoff Landing Air Taxi," *AIAA* Paper No. 2019-1948.
- [37] de Graaf, S., Pontika, E., Schmiede, P., Tong-Yette, F., Kazula, S., Ewald, D., and Isikveren, T. A., 2026, "Assessment of Novel Hybrid-Electric Propulsion System Based on Solid Oxide Fuel Cell and Gas Turbine," *ASME* Paper No. GT2026-178221.
- [38] Grimshaw, S. D., Pullan, G., and Walker, T., 2015, "Bleed-Induced Distortion in Axial Compressors," *ASME J. Turbomach.*, **137**(10), p. 101009.
- [39] Liu, B., Zhuang, X., An, G., and Yu, X., 2023, "Numerical and Experimental Study of Bleed Impact in Multistage Axial Compressors," *Chin. J. Aeronaut.*, **36**(2), pp. 1–16.
- [40] National Institute of Standards and Technology, 1996, "Water, NIST Standard Reference Database 69: NIST Chemistry WebBook," National Institute of Standards and Technology, Gaithersburg, MD, accessed Feb. 17, 2026, <https://webbook.nist.gov/cgi/cbook.cgi?ID=C7732185&Mask=4#Thermo-Phase>
- [41] National Institute of Standards and Technology, 1996, "NIST Chemistry WebBook," National Institute of Standards and Technology, Gaithersburg, MD, accessed July 1, 2024, <https://webbook.nist.gov/chemistry/>